JUNE 1955

THE WELDING JOURNAL

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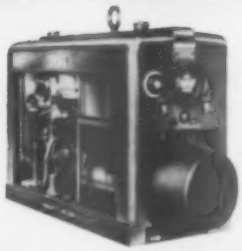

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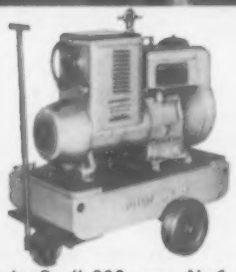

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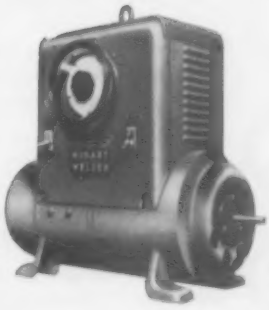

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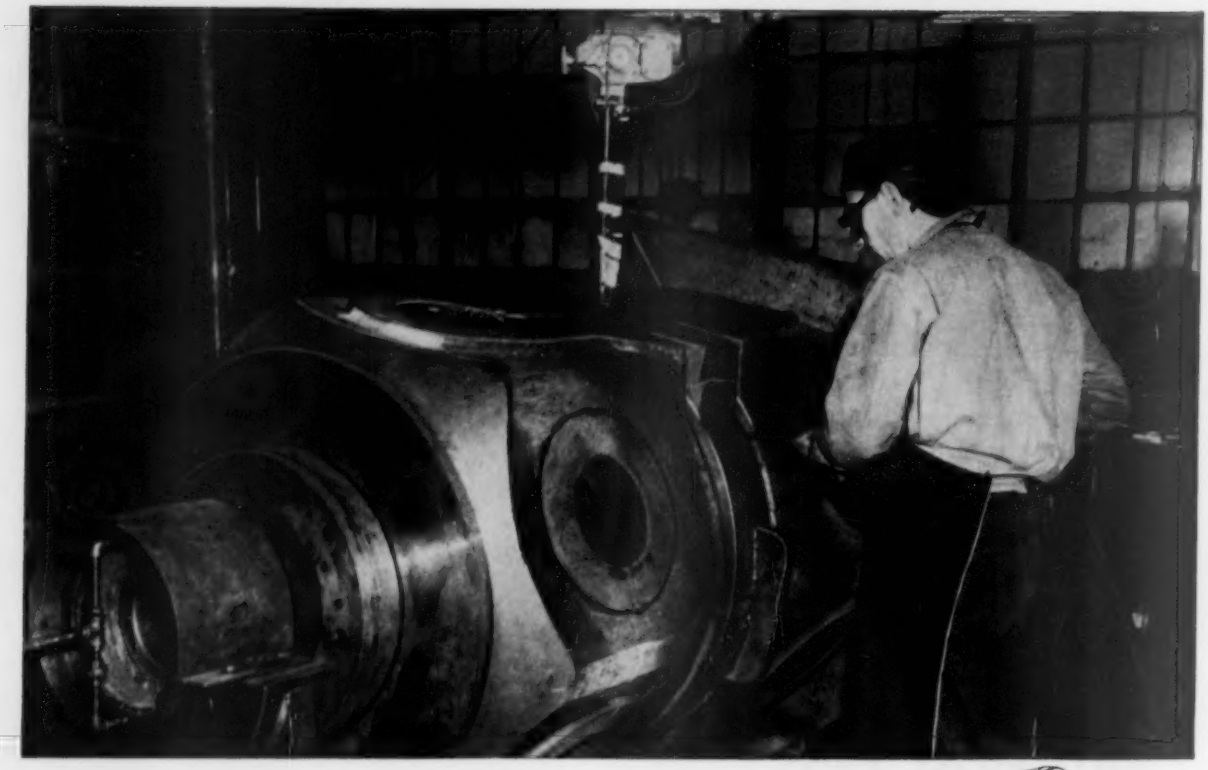

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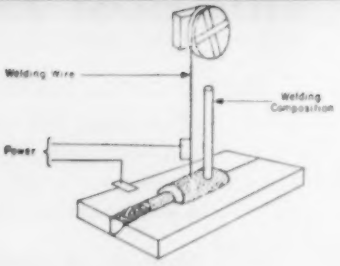

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THE WELDING JOURNAL

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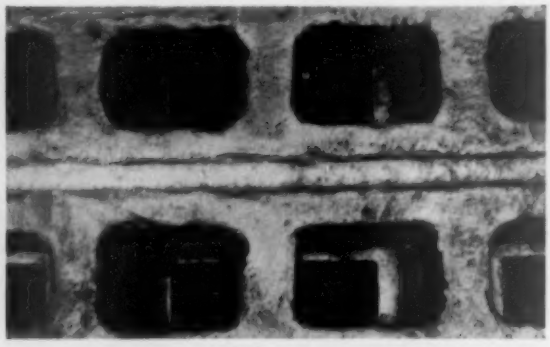

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EDITORIAL

THE ASME BOILER CODE AND WELDING

The ASME Boiler and Pressure Vessel Code primarily is a safety Code. It is based on years of experience in the design, fabrication and use of boilers and pressure vessels. By general acceptance at home and recognition abroad, it has become largely the law of the land here and an honored reference there.

Because it is a safety Code, it can neither be up on nor lag far behind the firing line of progress. Dynamic it must be in response to the changing needs of industry; if ever it becomes static it will be dead. Almost continuous interpretations, revisions and additions maintain its position of leadership.

The ASME Boiler and Pressure Vessel Committee took its first serious look at welding about the same time that the AMERICAN WELDING SOCIETY was born in 1919. Only part of the background of experience in riveted construction was pertinent to the formulation of welding rules. During the early bare wire days acceptance of this new method of fabrication was slow even though its enormous potentialities were visioned. The prospect of liquid and gastight joints with no restrictions on thickness staggered the imagination and spurred the mind.

Utilization of coincident increasing pressures and temperatures promised new industries in the petroleum and chemical fields and greatly increased efficiency in steam operations. The appearance of covered electrodes made the green light burn brightly.

The Code took into account these new developments and with suitable safeguards literally advanced with welding. These safeguards, however irksome to advocates of particular processes, assured stable growth and general acceptance of those processes, by fabricators and users alike.

New or improved processes, materials and methods provide the incentives and means of progress. The Boiler and Pressure Vessel Committee continues to look to the Welding Industry through the technical committees of the AMERICAN WELDING SOCIETY and the research committees of the Welding Research Council for further economies, improvements and new horizons—all with no sacrifice of safety. Everyone identified with the Welding Industry can help in this advance. Everyone has a stake!

H. C. Boardman
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The appropriate TEMPILSTIK° is selected and, for most applications, the work-piece is marked with it before heat is applied. On subsequent heating the crayon-like mark of the TEMPILSTIK° melts as soon as its temperature rating is reached.

The mark made by a TEMPILSTIK° may gradually change in color as heating progresses. The change in color, or in color intensity, is never to be interpreted as a temperature indication. The TEMPILSTIK° mark melts when its temperature rating is reached and only this change from the dry to the liquid condition is the significant temperature signal.

In some applications this simple method may not suffice because evaporation of the TEMPILSTIK° mark on prolonged heating, or its gradual absorption by the surface at high temperatures, may leave too little residual substance for unambiguous observation. In such cases stroking or touching the work-piece with the TEMPILSTIK° at regular intervals during the heating process is recommended. The TEMPILSTIK° will leave a dry mark at temperatures below its rating and a liquid streak when its temperature rating has been reached or exceeded. If the workpiece is not accessible for applying a TEMPILSTIK° during the heating process, then TEMPILAQ°, or possibly TEMPIL° PELLETS, should be used instead.

TEMPILSTIKS° will usually glide over unheated polished surfaces (like glass, polished stainless steel, etc.) without leaving an appreciable mark. Therefore, in applications that involve smooth or polished surfaces TEMPILSTIKS° should be used by touching or stroking the workpiece periodically during the heating process, as previously described, because a liquid streak will be made on contact as soon as the specified temperature is reached. Where it is necessary to mark a smooth work piece surface prior to heating, TEMPILAQ° should be used.

A hot radiating surface will appear relatively dark under intense illumination from an external source, and this fact can be utilized to improve recognition of the temperature signal against a red-hot background. The use of TEMPIL° PELLETS or TEMPILAQ° instead of TEMPILSTIKS° will further improve visibility under these conditions.

On rapidly moving objects, like magnesium or aluminum sheet during spinning operations, it is impossible to see whether the applied TEMPILSTIK° leaves a dry or melted mark. However, with a little experience operators quickly learn to sense the smooth gliding of a TEMPILSTIK° over a surface which is hot enough to melt it on contact, as contrasted to the frictional drag on a cooler surface. If the top of the TEMPILSTIK° is brought in contact with the moving metal surface for a brief moment at a time, and with light pressure only, the heat contribution of friction is trivial enough to be ignored.

The above described "sensing technique" may also be employed on hot radiating surfaces where the bright background makes it difficult to distinguish whether or not the TEMPILSTIK° mark had melted.

An appropriate series of TEMPILSTIK° marks, applied to the workpiece or surface under investigation before heating begins, will provide a record of the maximum temperature attained during a process or operation. Subsequent examination will show that all TEMPILSTIK° marks up to a certain temperature rating had melted, while those of a higher rating had not. The maximum attained temperature must consequently lie somewhere in the interval between the highest melted and the lowest unmelted TEMPILSTIK° rating.

The temperature distribution and isothermal boundaries of surfaces such as furnace walls, melting ladles, cement kilns, etc., can be established by using an appropriately chosen series of TEMPILSTIKS° to draw a pattern of lines (parallel, concentric, radial, etc.) on the area under investigation. The heat conduction along a surface can be effectively studied by the progressive melting of suitably chosen TEMPILSTIK° lines. Caution must be exercised not to permit the mark made with one TEMPILSTIK° rating to cross or overlap the mark of a dissimilar TEMPILSTIK°, as such a mixing of different TEMPILSTIK° marks would destroy their accuracy.

TEMPIL° products will give good results in induction heating and in ionized air, as well as in the presence of static electricity about electrical equipment, where electrical means of measuring temperatures often function erratically.

Fig. 1 Typical application showing use of semiautomatic submerged-arc-welding equipment

HOW TO USE SEMIAUTOMATIC SUBMERGED ARC WELDING

By R. A. WILSON

Where process is indicated and properly executed, there are many advantages to be gained through its application

R. A. Wilson is Director of Application Engineering, The Lincoln Electric Co., Cleveland, Ohio.

Paper was presented at the AWS 1954 National Fall Meeting held in Chicago, Nov. 1–5, 1954.

The semiautomatic submerged arc process—with many outstanding advantages over conventional hand welding methods including speeds up to 4 times greater—is every year finding wider and wider application, both in the shop and in the field.

In hundreds of instances, the semiautomatic method is being adapted to very profitable use as fabricators become increasingly aware of its potentialities and operators develop, in many cases, unbelievable skill in its handling. On one outstanding current project in which more than $2^1/_2$ million lineal feet of welding is involved—construction of the new $15,000,000 skyway in San Francisco—it is noteworthy that almost 60%

relatively dark under intense illumina-

tion erratically.

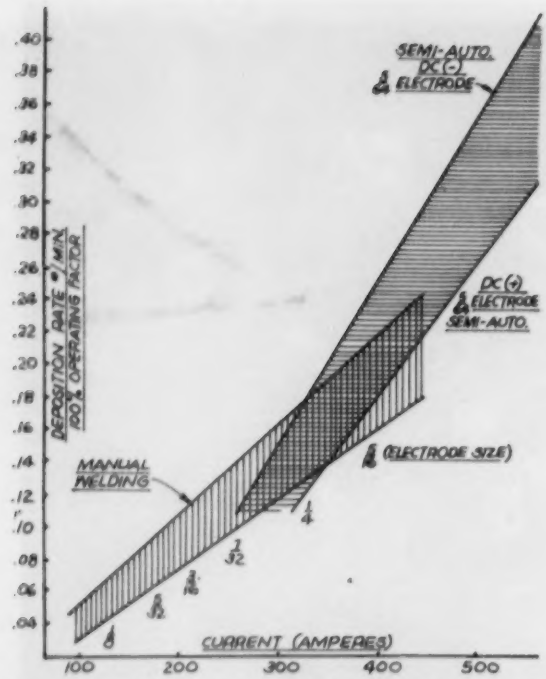

Fig. 2 Deposition rates—manual electrodes versus semiautomatic

of this great footage is being done by semiautomatic submerged arc welding, much of it in the shop.

Equipment

The available equipment for this process is semiautomatic in that the electrode is a continuous wire which is fed to the work mechanically. It is called submerged arc because a granular flux composition is spread just ahead of the electrode and the arc is invisible under this flux.

A typical application is shown in Fig. 1. In the operator's hand is the conical flux container and welding nozzle through which the electrode is fed. A separate unit contains the continuous electrode supply, the motor used to power it, and the controls for feeding the electrode and for maintaining the proper arc voltage. In this model, the control unit can be separated from the wire reel for better portability.

The source of welding power is usually a d-c generator, although some makes use a rectifier or a-c transformer.

D-c positive polarity will provide (1) the least porosity, (2) the best penetration, (3) the least undesirable build up, and (4) usually the best bead shape. D-c negative polarity will provide (1) the highest melt-off rate, which, of course, means the fastest welding, (2) the least burn through, and (3) less plate admixture—less cracking and better hardfacing. A-c power is used chiefly where arc blow is a problem.

Because the arc is completely submerged, there is no smoke or spatter and the welds are exceptionally smooth. As the arc moves across the work, the flux melts, then solidifies on the surface. This surface slag is very easy to remove—in many instances it will crack off by itself on cooling—thus making for lower cleaning costs after welding. There is no spatter whatsoever to adhere to the plate, further reducing the normal cleaning costs for hand welding.

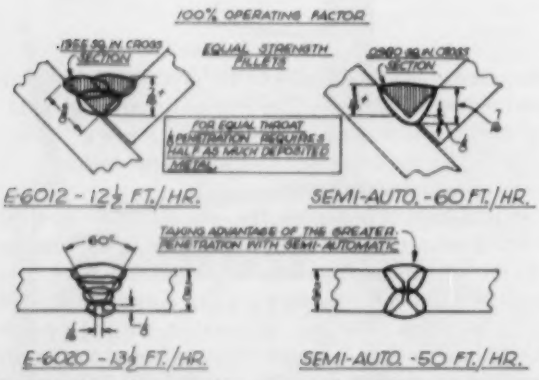
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Fig. 3 Speed comparison—manual welding versus semiautomatic

Advantages of Process

Since the arc and molten metal are covered by flux at all times, air never comes in contact with the weld, assuring the highest quality of weld metal.

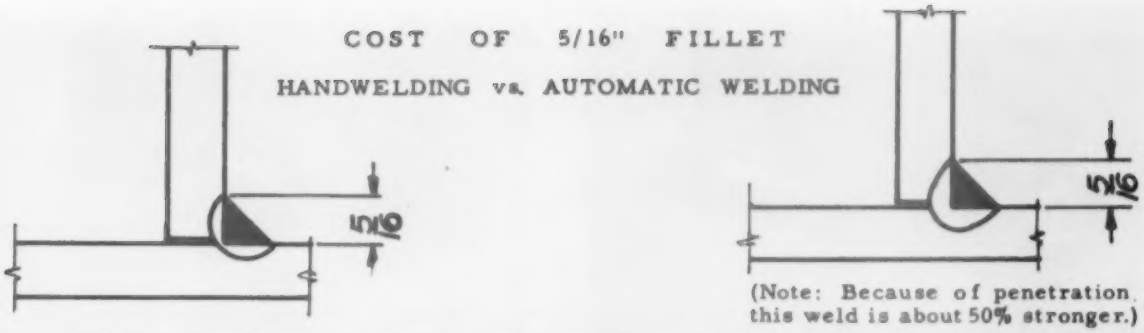
One of the great advantages of semiautomatic welding is the very high melting rate at high currents. High current density in the range of 50,000 to 75,000 amp per sq in. gives a very fast melting rate with the usual $^{5}/_{64}$-in. electrode—much higher than with the same current on a $^{1}/_{4}$-inch fully-automatic electrode or manual electrode.

Comparing the melting rate with that of hand welding, the chart in Fig. 2 shows that semiautomatic welding starts where hand welding leaves off, going as high as double the deposition rate obtained with the best hand welding practices.

The hand welding range shown in Fig. 2 covers the E6015-E6010 electrodes on the bottom line to iron-powder electrodes on the top of the shaded area. In the semiautomatic range, the lower melting rate occurs when electrode is positive and the higher melting rate when electrode is negative. It will be noted that electrode negative is as much as 45% higher in deposition rate than positive polarity at these current densities.

The combination of higher melting rate and greater penetration obtained enables vast increases in speeds for equal strength with semiautomatic over handwelding. Speed comparisons of the two processes are shown in Fig. 3. Both deep penetrated fillets and butt welds are compared here.

The chart in Fig. 4 shows the cost advantages gained by use of the higher melting rate of the semiautomatic process. Not included, of course, is the added bonus of a stronger weld. It is interesting to note that it takes



(Note: Because of penetration this weld is about 50% stronger.)

1/4" (E-6012)	1/4" Iron Powder (E-6024)	Electrode Size, make, type, AWS class	Semi Automatic 5/64"	Full Automatic 5/32"
350 AC	350 AC	Current - Polarity	475 Neg.	700 Pos.
.13	.20	Melt-off Rate (lbs./min.)	.32	.34
10"	14"	Arc Speed in./min.	20"	24"
50'	70'	Arc Speed ft./hr.	100'	120'
25'	35'	Arc Speed x % operating factor actual ft./hr. (50% O.F. used)	50'	60'
.30	.30	Electrode Used lbs./ft. (see note "A")	.20	.20
.12	.17	Electrode Cost $/lb.	.22	.20
$5.00	$5.00	Labor & Overhead $/hr. (2.50) (100% labor cost)	$5.00	$5.00
$.236	$.194	Total Cost per foot (see note "B")	$.144	$.123
–	$.042	$ Savings per foot	$.092	$.113

Note "A" - List total rod (in lbs.) used, including stub.
(Takes into account deposition efficiency and bead shape.)

Note "B" - $\frac{\text{Total Cost}}{\text{Ft.}} = \frac{\text{Electrode Cost}}{\text{Ft.}} + \frac{\text{Labor \& Overhead}}{\text{Ft.}}$

(Cost of Power has been eliminated as being approximately same in all cases and too small to consider.)

Fig. 4 Cost comparisons—hand welding versus semi- and fully automatic

700 amp to get about the same melting rate with large wire on the fully automatic as compared to 450 amp on the $^{5}/_{64}$-in. semiautomatic electrode.

There are several other not-inconsiderable advantages of semiautomatic welding. Since there is no visible arc, no shield is required. Also, because there is less spatter and no smoke, the operating factor is increased and there is less operator fatigue. Moreover, the operator can acquire the necessary skill for his work with less training than required with other processes.

In comparison with fully-automatic welding, the semiautomatic method has these advantages: (1) it can manually follow irregular shapes, (2) no fixtures, or only simple fixtures, are required, (3) it can handle a great variety of jobs and is easily portable, and (4) its first cost is lower.

The two welds shown in Fig. 5. are typical of the contours that the semiautomatic equipment can follow with perfect guiding that would be impossible to line up for fully-automatic operation. One "drag" fillet is on an S-shaped test plate; the other is on a curved tank bottom.

Practices and Procedures

The semiautomatic equipment can be used in any of these four broad applications:

1. where the welding gun can be dragged along the joint, providing accurate guiding
2. where the work can be rotated and the gun held in position by hand
3. where the work can be rotated and the gun held in a simple locating fixture

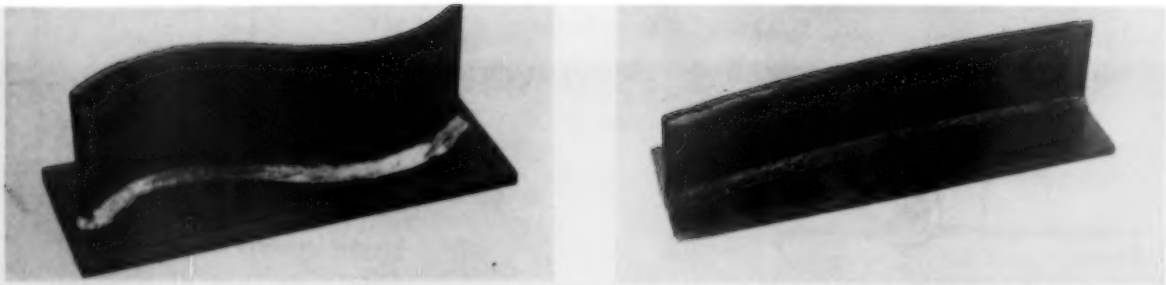


Fig. 5 Typical contours that the semiautomatic can follow with perfect guiding—an S-shaped test plate (*left*) and a curved tank bottom (*right*)

4. where both the gun and the work can be moved by special fixtures.

In each of these applications, experience has pointed out certain practices and procedures which should be followed in order to take full advantage of their potentialities.

In changing from hand to semiautomatic welding, the deep narrow V's normally used should be changed to wider and shallower grooves, as shown in Fig. 6. The semiautomatic has plenty of penetration to bite into the bottom of the shallow groove without a chance of slag interference and better bead shape results.

The correct way to guide the gun while dragging it along the stationary work is demonstrated by the operator in Fig. 7.

In this type of setup, the standard $^5/_8$ in. ID cone tip is usually used to conserve flux and on some applications, all the flux deposited is fused, none having to be picked up after welding for re-use. However, when using the drag technique, especially for fillet welding, it is often desirable to have a larger opening in the copper cone tip to permit a greater flow of flux for better arc coverage and also to keep the electrode nozzle at a greater distance from the molten metal (see Fig. 8). In normal operation the enlarged tip is dragged, touching both plates. The cone should be tilted no more than 15 to 20° in the direction of travel.

When butt welds are made the cone tip may be dragged on the first pass, but often, for subsequent passes, the gun must be raised to get proper flux coverage and this requires guiding by eye. As shown in Fig. 9, it is best to throw some flux in the V'd joint for its full length before welding, as this will keep the molten metal from running ahead of the arc.

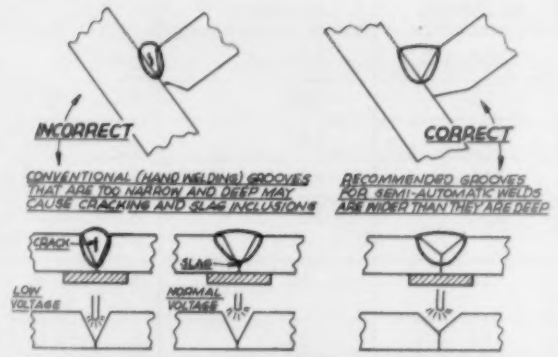

INCORRECT
CORRECT
CONVENTIONAL (HAND WELDING) GROOVES THAT ARE TOO NARROW AND DEEP MAY CAUSE CRACKING AND SLAG INCLUSIONS
RECOMMENDED GROOVES FOR SEMI-AUTOMATIC WELDS ARE WIDER THAN THEY ARE DEEP
CRACK
SLAG
LOW VOLTAGE
NORMAL VOLTAGE

Fig. 6 Grooves for single-pass semiautomatic welding

Fig. 7 Operator on heavy base welding, demonstrating the correct way to guide the welding gun

Typical Applications

Figure 10 shows a typical application where the work is rotated and the gun is held in position by hand. This is perhaps the ideal application of true semiautomatic welding because it combines the ability of manual dexterity to compensate for poor fit-up and irregularities in forming with the higher speeds and more uniform welding made possible by the continuously fed electrode and mechanical rotation of the work.

Figure 11 shows a particularly interesting application of this type of setup in that its result is a markedly increased duty cycle.[4] Instead of loading and welding parts one by one, the operator stacks a half dozen parts on a mandrel and welds them consecutively in a more efficient sequence.

Fig. 10 Typical application where the work is rotated and the gun is held in position by hand

Fig. 9 When butt welds are made, the cone tip may be dragged on the first pass, but often the gun must be raised to get proper flux coverage and this requires guiding by eye

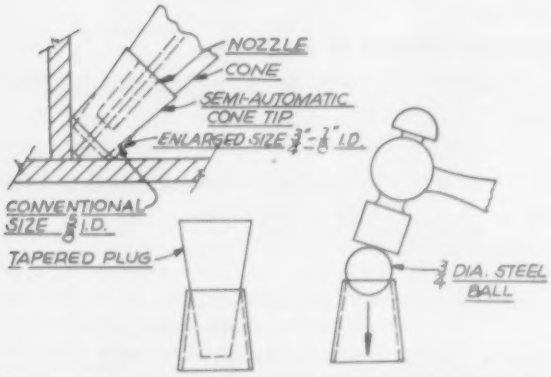


Fig. 8 Enlargement of the cone tip when using the drag technique

Fig. 11 Welding inverted flange commutator caps. Half a dozen parts are stacked on a mandrel at once to speed up the operation

Fig. 12 Typical application where the work is rotated and the welding gun is held in a simple fixture

This operation is in the plant of the Lincoln Electric Co., where the operator, being on piecework, has the choice of either hand welding or the semiautomatic. There is no doubt as to which process he prefers. These welds are made at a speed of 50 ipm. The positioner is horizontal for loading and is swung to the welding position at the touch of a foot pedal. The turntable is counter-balanced and held in any position with a foot-operated brake.

Most of the jobs in the Lincoln plant are of the short weld, production type, which are normally considered hand welding's bailiwick. Even without the opportunity for long runs and high currents, savings of about 30% in setting up a job for semiautomatic welding are usually expected.

Figure 12 shows a typical application where the work is rotated and the gun is held in a simple fixture. The addition of the pivoted arm to support the cone makes this setup practically fully automatic. It allows the operator to keep both hands free and to knock off the slag when making the overlap—a matter of importance when making pressure-tight welds such as this heat exchanger part; also when making such things as hydraulic cylinder flanges, pipe flanges and the like.

Figure 13 is another example of this type of application. With the proper brackets and adjustments to hold the gun, precision welding is easily accomplished here. Quite often the same generator setting can be used with a hand electrode to do the tacking and assembling, the operator merely picking up either the electrode holder or gun as needed. Note the brush used to support flux on the corner weld.

On mechanized setups where the gun is mounted in a fixture, the following simple starting method has proved best:

1. Have the gun mounted on the pivoting arm with a stop to hold it at the proper height above the work.
2. Inch the electrode out and cut off so that the gun will rest on the electrode and will be held $^1/_8$ to $^1/_4$ in. above the normal position of stop when the gun is lowered to start welding.
3. Lower the pivoting arm until the wire touches the work, and when the electrode contacts the work, the arc starts. The gun will then continue to drop the $^1/_8$ to $^1/_4$ in. until the arm hits the stop. The downward movement of the gun will feed the wire the instant it starts to melt off and keep it from burning back before the wire feed motor gets up to full speed. This will prevent the arc from lengthening and going out or possibly fusing to the nozzle tip.
4. If possible, have the work or gun traveling when contact is made as better striking is obtained this way.

In addition to the applications herein described and those illustrated in Figs. 14 to 17, there are many other ingenious applications attesting to the flexibility of the semiautomatic process.

In some quarters, however, this process has not been used to maximum advantage. There are apparently two chief reasons for this: (1) semiautomatic welding is misapplied on applications much better suited for other processes, and (2) in instances where it replaces well-established hand welding procedures, the operator is not given time enough to become equally well-skilled with the new method.

In the latter case, the problem is simply one of training and practice over a reasonably short period of time. After a month's work with the semiautomatic, many operators gain unbelievable skill on jobs that at first attempt seemed hopeless. The same operator who bitterly complained that he couldn't achieve his production rate because he was continually fusing tips, missing the seam, or burning through, soon becomes adept, and if the application is well chosen, would not think of going back to hand welding.

But the key to the matter is in that phrase "well chosen." The most important factor in achieving a successful semiautomatic welding program is the proper evaluation of each job and the right choice between hand, semiautomatic and fully-automatic welding.

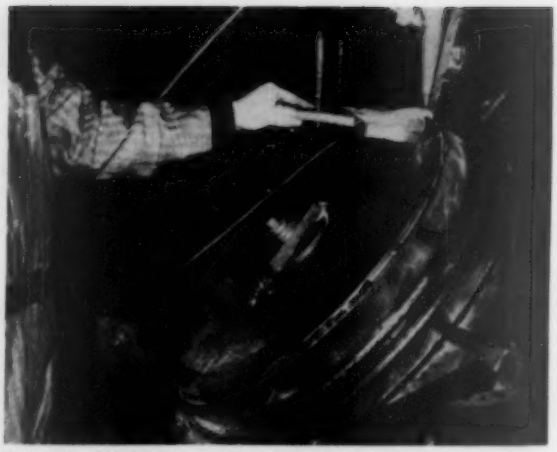


Fig. 13 With the proper brackets and adjustments to hold the gun, precision welding is easily accomplished on this type of work. The brush is used to support flux on the corner weld

Fig. 14 Shear lugs being welded to large bridge girders with the drag technique. This is on the San Francisco Skyway project where almost 60% of the $2^1/_2$ million lineal feet of welding involved is being done with this process

General Spheres of Suitability

In general, the three processes have the following spheres of suitability:

A. Use hand welding:
   1. On out-of-position welding where a large molten pool would spill.
   2. For very short welds.
   3. For light gage welding. (About 10 gage material is considered the lower limit for semiautomatic applications, but there are undoubtedly many jobs being done on still lighter gages.)
   4. On small non-repetitive jobs.

Fig. 15 Where large volumes of metal are involved, there is little question of the advantages of the semiautomatic process. This is alloy steel being used to form the lip of a huge drag line bucket where groove welds 6 in. thick are made to attach the teeth

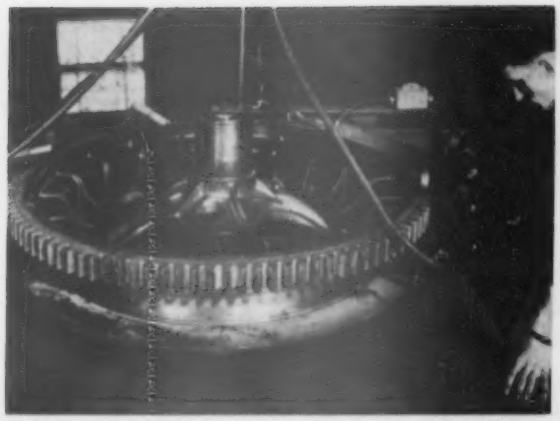


Fig. 16 Where the work is too large to be rotated on a positioner, the gun can be mechanized to travel on the work, as shown in the case of this revolving turret gear

Fig. 17 A simple boom fixture such as this one can be developed for welding inside of tanks, pipe or other awkward shapes. The equipment would be heavier and bulkier if a full-automatic head were used

5. For jobs where fit-up cannot be controlled.
6. For applications where it would be difficult to retain flux because of the shape of the work.

B. Use fully-automatic welding wherever practical and economical. When complete automation is desired, this method provides:

1. Push button automatic arc striking.
2. Heavier equipment construction.
3. Higher current capacity.
4. Self-contained travel mechanism.

The above reasons usually make the fully-automatic process the choice for highly repetitive jobs that can be fixtured.

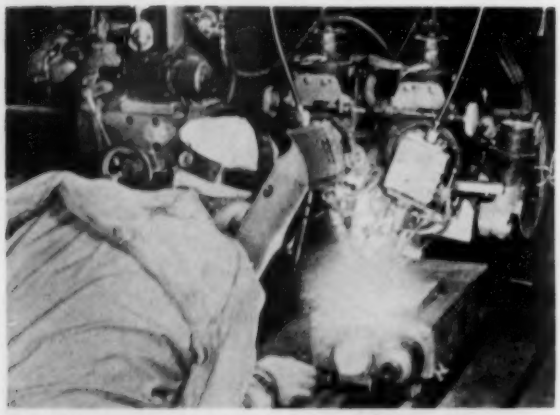
C. Use semiautomatic welding:

1. On jobs where you can take advantage of the increased melting rate at the higher currents used.
2. Where the continuous wire feed will increase the duty cycle.
3. For work that is repetitive enough so that proper procedure and practice can be worked out until proper skill is acquired.
4. Where complicated shapes make fixturing for fully-automatic too difficult.
5. Where the additional penetration is an advantage.
6. Where contour and fit-up is not accurate enough for fully-automatic guiding.

Conclusions

The choice facing most users is to decide between hand welding and semiautomatic and then to make proper use of semiautomatic when applied. The illustrations used in this paper show where and how best to use the semiautomatic process. Where the process is indicated, where it is properly applied, and where the operator is given a chance to develop sufficient skill with it, there is no question about the great advantages to be gained.

THE INERT-GAS METAL-ARC OVERLAY PROCESS



Depositing a stainless steel overlay with the series arc technique

Overlaying normally requires minimum penetration or dilution for all types of surfacing applications. Author discusses the many factors that affect penetration with inert-gas metal-arc welding

BY C. R. FELMLEY

ABSTRACT. An investigation was made of the welding variables which affect the penetration and dilution of overlays deposited by the inert-gas metal-arc-welding process with a consumable electrode. The influence of each of the welding variables was shown by depositing comparable single-arc bead welds where all of the other variables were held constant.

In order to obtain increased deposition rates and still maintain minimum dilution and penetration, multiple-arc techniques were developed that incorporated the addition of an auxiliary filler wire to each arc and oscillation of the whole welding unit.

In general, single-arc operations with straight-polarity direct current or alternating current produce less dilution and penetration than reverse-polarity direct current. Minimum welding current in conjunction with longer arc lengths produces a less forceful arc plasma which is conducive to less penetration. Increasing the stick-out of the electrode wire and the use of argon as a shielding gas aid in producing minimum dilution.

The a-c series arc technique which employs multiple-arcs with activated electrode wires, the addition of auxiliary wires and oscillation of the whole welding unit produced overlays that had minimum dilution and penetration at very high deposition rates. Satisfactory multiple-arc overlays, with minimum dilution and penetration, were made with copper base electrode wires using reverse-polarity direct current.

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This paper was presented at the AWS National Fall Meeting held in Chicago, Nov. 1–5, 1954.

Introduction

Fusion welding can be used to deposit a layer of metal onto the surface of a different base metal to provide resistance against corrosion and wear. Close control of the overlay composition is necessary to obtain satisfactory weldment performance under severe conditions of service.

Control of overlay composition is strongly influenced by the efficiency with which elements are transferred from an electrode or filler rod to the deposited layer. Excessive loss of alloying elements by oxidation must be compensated by an increase in the alloy content of the electrode or rod which therefore becomes more expensive. Excessive oxidation also may modify adversely the characteristics of the protective layer.

Since uncontrolled oxidation is undesirable, inert-gas-shielded metal-arc consumable-electrode welding appears to offer great promise as a surfacing method. With pure inert gas, no oxidation loss occurs; and with additions of oxygen to the inert gas, the degree of oxidation is consistent and controllable. However, this inherent advantage of inert-gas-shielded welding can be lost if improper application of the process melts too much base metal or produces inconsistencies in the amount melted.

Because the base-metal composition differs from that of the deposited layer, melting of the base metal must be minimized to prevent alteration of the overlay characteristics obtained from any given rod or electrode. When a relatively soft metal is deposited on a hard base, alloying caused by excessive base-metal melting can produce scattered hard spots in the deposited layer. An example of this phenomenon sometimes is encountered when a copper overlay is placed on high-carbon steel. Conversely, a hard layer can be softened by excessive dilution which reduces the percentage of various constituents controlling hardness below the required minimum. This adverse effect of dilution becomes more troublesome as the thickness of the overlay is decreased. That corrosion resistance can be lowered by too much melting of the base material is best illustrated by the well-known deterioration of a stainless steel overlay if the carbon pickup from the carbon-steel base plate is excessive. Such lowered resistance often is avoided by the deposition of several layers;

but if dilution can be kept low, a more economical thin layer may be used.

To minimize dilution and penetration with the inert-gas process, the process variables must be carefully controlled; therefore, a study of the influence of the most important variables was undertaken. These included type and magnitude of current, arc voltage, welding speed, length (stick-out) of electrode protruding from the contact tube, shielding-gas composition, electrode activation, number of arcs, electrode oscillation, and the use of auxiliary filler wire. Without accumulating a large quantity of data on each variable, sufficient information was obtained to indicate the trend of dilution and penetration as the process variables were modified. It is hoped that this information will provide a rough guide for the application of the process to surfacing.

The investigation of process variables was restricted to carbon steel and stainless steel electrodes; however, several other materials were deposited to demonstrate the versatility of inert-gas metal-arc welding. In addition to its versatility, the process produces slag-free deposits at a high deposition rate.

Test Materials and Equipment

All of the bead welds and overlays were made with $^1/_{16}$ in. diam electrodes on 6- x 9- x 1-in. plate. Heavy plate was used to eliminate warpage during the welding operation. The chemical analysis of the plate was as follows: C, 0.25%; Mn, 1.17%; P, 0.013%; S, 0.022%; Si, 0.46%; Ni, 0.61%; Cr, 0.66%; Mo, 0.33%. The electrodes had the following compositions:

1. A675 (carbon steel)—C, 0.12%; Mn, 0.95%; P, 0.025%; S, 0.035%; Si, 0.45%.
2. A310 (Type 310 stainless steel)—Cr, 25%; Ni, 20%; Mn, 1.5%; C, 0.2%.
3. A683 (nickel-manganese steel)—C, 0.8%; Mn, 14%; Ni, 3.5%; Si, 1.1%; P, 0.04%; S, 0.04%.
4. A261 (Type 61 nickel)—Ni, 93%; Ti, 3%; Al, 1.5%; Mn, 1%; Fe, 1%; Si, 0.75%.
5. A145 (deoxidized copper)—Sn, 0.8%; Si, 0.25%; Mn, 0.2%; Cu, Bal.
6. A158 (aluminum bronze)—Al, 8%; Zn, 0.2%; Si, 0.1%; Cu, Bal.

The carbon steel, stainless steel, nickel and nickel-manganese steel wires were coated with an activating agent for straight-polarity direct-current (DCSP) and alternating-current welding. The activating agent was a very thin current-conducting addition that modified the metal transfer and arc characteristics. Without this coating, it would have been difficult to develop a series-arc welding technique with the consumable-electrode inert-arc process. The copper overlays were made with reverse-polarity direct current (DCRP) since the multiple-arc techniques produced low penetration and dilution without the activated coating.

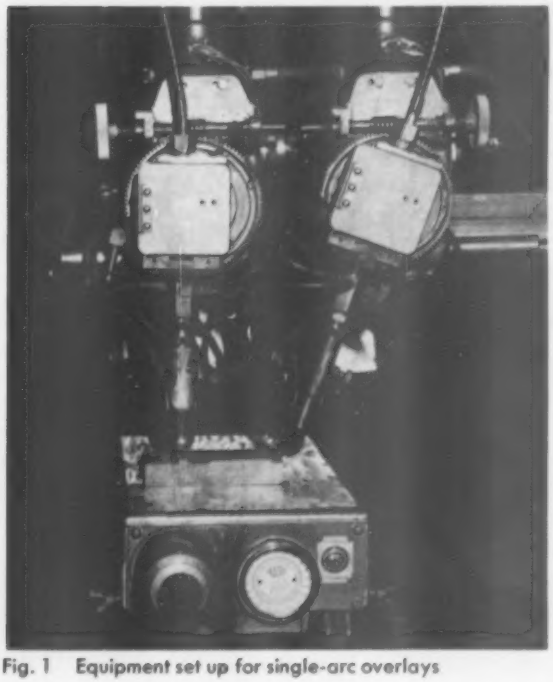


Fig. 1 Equipment set up for single-arc overlays

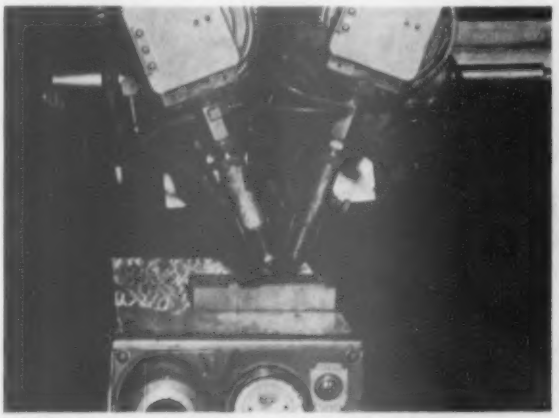


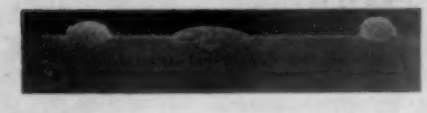
Fig. 2 Equipment set up for multiple arc overlays

The experimental welding equipment consisted of two semiautomatic welding heads with constant wire feed and two auxiliary wire feeders. All four units were mounted on an oscillating mechanism. The equipment was designed to operate with either direct current or alternating current, using either one or two arcs with or without the addition of an auxiliary wire. With the oscillating mechanism, layers up to $1^3/_4$ in. in width could be deposited. Commercially available alternating current transformers or direct current motor generator welding current sources were employed.

Test Procedure

For bead welds made with a single arc, the equipment was adjusted as in Fig. 1 for use without an auxiliary wire or oscillation. The test plate was moved under the

CARBON STEEL ELECTRODE - A675



TYPE OF CURRENT	AC	DCRP	DCSP
CURRENT (AMPERES)	300	300	300
VOLTAGE (VOLTS)	21	29	21
WIRE DEPOSITION (LBS./HR.)	9.3	9.6	10.1
DILUTION (%)	31	41	30
PENETRATION (INCH)	0.10	0.13	0.08

TYPE 310 STAINLESS STEEL ELECTRODE -A310

TYPE OF CURRENT	AC	DCRP	DCSP
CURRENT (AMPERES)	295	295	300
VOLTAGE (VOLTS)	21	30	21
WIRE DEPOSITION (LBS./HR.)	12.3	10.2	11.5
DILUTION (%)	29	42	34
PENETRATION (INCH)	0.11	0.16	0.12

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH.
WELDING SPEED: 12 IN./MIN.
A.C. AND STRAIGHT POLARITY D.C. ELECTRODES WERE COATED WITH AN ACTIVATING AGENT.

Fig. 3 Effect of type of current on a bead weld

arc at the desired welding speed by the carriage. The temperature of the test plate was below 100° F prior to any welding operation.

The setup of equipment for multiple-arc overlays is shown in Fig. 2. Two auxiliary wires were employed to increase the deposition rate and to help minimize weld penetration. Oscillation was employed to increase the overlay band width and to obtain more uniform dilution.

The test plates were sectioned and photographed to compare the bead appearance and determine the dilution and penetration obtained with the various welding conditions. A polar planimeter was used to measure the cross-sectional area of the weld deposit in order to calculate the amount of dilution as follows:

$$\% \text{ dilution} = \frac{\text{Area } A}{\text{Area } (A + B)} \times 100$$

where A is the cross-sectional area of the weld bead below the surface of the base metal, and B is the cross-sectional area of the weld bead above the surface of the base metal. The penetration of each weld deposit was measured to the nearest 0.01 in.

Results

The results of this investigation are shown in Figs. 3 through 12. The data of Fig. 3 were repeated in Figs. 4 through 7 for easier comparison of weld cross sections.

Type of Current (AC, DCRP, DCSP)

The data in Fig. 3 show the effect of type of current upon the appearance of a bead weld made with either a carbon steel or stainless steel electrode. The data indicate the following:

1. In consumable-electrode inert-arc bead welds with either carbon or stainless steel electrodes, the greatest dilution and penetration is produced by DCRP if all of the other welding conditions are comparable. (Note the papillary obtained with this type of current.)
2. For both the carbon and stainless steel electrodes, better wetting and less reinforcement are obtained with DCRP than with DCSP or AC.
3. The deposition rate of stainless steel was larger than that obtained for carbon steel. This was due to the greater resistance heating and lower melting temperature of Type 310 stainless steel.

Magnitude of Current

Figure 4 shows the effect of increasing the welding current approximately one hundred amperes for all three current types with carbon and stainless steel electrode wires. This increase of about one-third of the

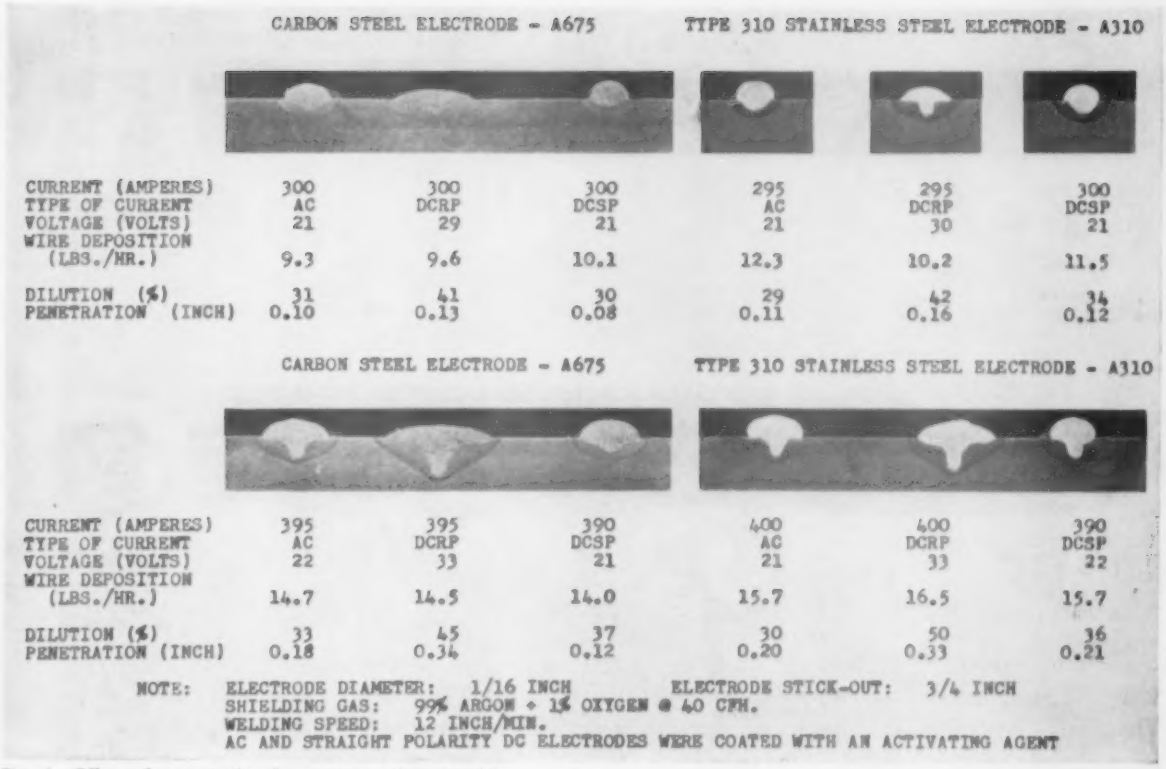

CARBON STEEL ELECTRODE - A675
TYPE 310 STAINLESS STEEL ELECTRODE - A310
CURRENT (AMPERES) 300 300 300 295 295 300
TYPE OF CURRENT AC DCRP DCSP AC DCRP DCSP
VOLTAGE (VOLTS) 21 29 21 21 30 21
WIRE DEPOSITION (LBS./HR.) 9.3 9.6 10.1 12.3 10.2 11.5
DILUTION (%) 31 41 30 29 42 34
PENETRATION (INCH) 0.10 0.13 0.08 0.11 0.16 0.12
CARBON STEEL ELECTRODE - A675
TYPE 310 STAINLESS STEEL ELECTRODE - A310
CURRENT (AMPERES) 395 395 390 400 400 390
TYPE OF CURRENT AC DCRP DCSP AC DCRP DCSP
VOLTAGE (VOLTS) 22 33 21 21 33 22
WIRE DEPOSITION (LBS./HR.) 14.7 14.5 14.0 15.7 16.5 15.7
DILUTION (%) 33 45 37 30 50 36
PENETRATION (INCH) 0.18 0.34 0.12 0.20 0.33 0.21
NOTE: ELECTRODE DIAMETER: 1/16 INCH ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH.
WELDING SPEED: 12 INCH/MIN.
AC AND STRAIGHT POLARITY DC ELECTRODES WERE COATED WITH AN ACTIVATING AGENT

Fig. 4 Effect of magnitude of current on a bead weld

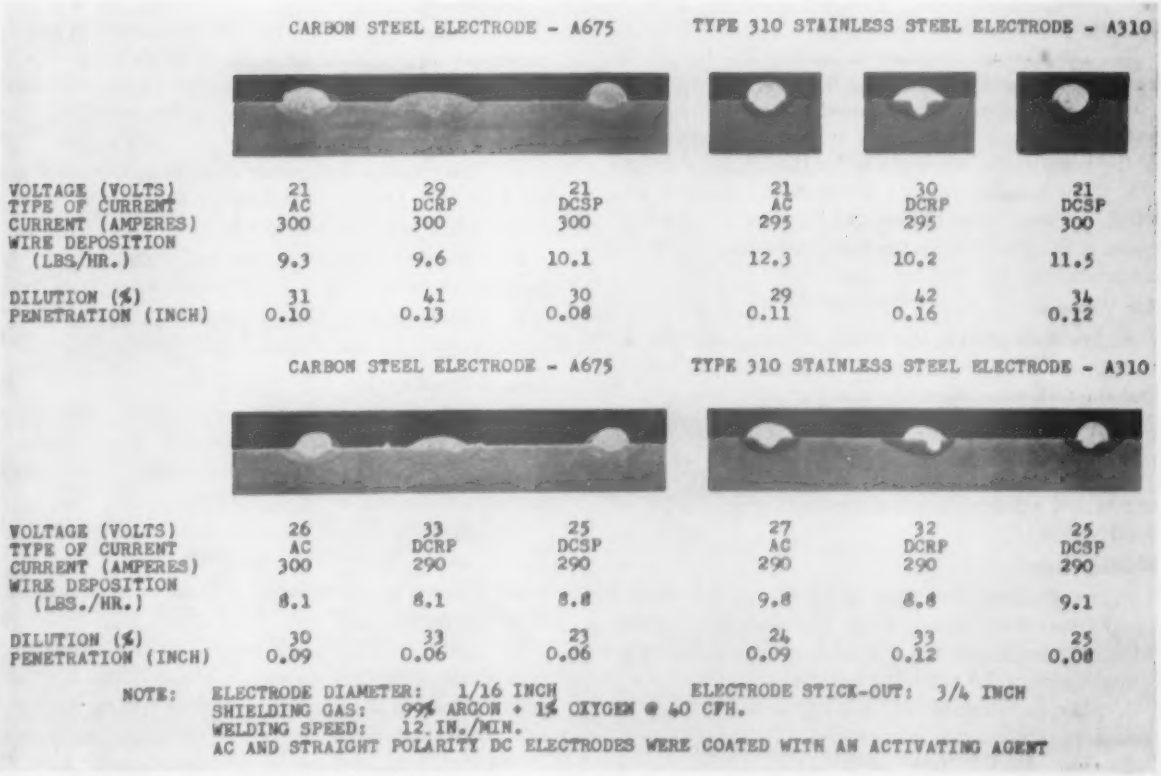
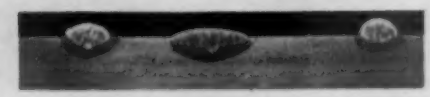
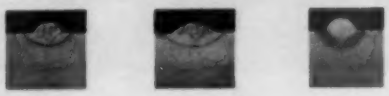

CARBON STEEL ELECTRODE - A675
TYPE 310 STAINLESS STEEL ELECTRODE - A310
VOLTAGE (VOLTS) 21 29 21 21 30 21
TYPE OF CURRENT AC DCRP DCSP AC DCRP DCSP
CURRENT (AMPERES) 300 300 300 295 295 300
WIRE DEPOSITION (LBS./HR.) 9.3 9.6 10.1 12.3 10.2 11.5
DILUTION (%) 31 41 30 29 42 34
PENETRATION (INCH) 0.10 0.13 0.08 0.11 0.16 0.12
CARBON STEEL ELECTRODE - A675
TYPE 310 STAINLESS STEEL ELECTRODE - A310
VOLTAGE (VOLTS) 26 33 25 27 32 25
TYPE OF CURRENT AC DCRP DCSP AC DCRP DCSP
CURRENT (AMPERES) 300 290 290 290 290 290
WIRE DEPOSITION (LBS./HR.) 8.1 8.1 8.8 9.8 8.8 9.1
DILUTION (%) 30 33 23 24 33 25
PENETRATION (INCH) 0.09 0.06 0.06 0.09 0.12 0.08
NOTE: ELECTRODE DIAMETER: 1/16 INCH ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH.
WELDING SPEED: 12 IN./MIN.
AC AND STRAIGHT POLARITY DC ELECTRODES WERE COATED WITH AN ACTIVATING AGENT

Fig. 5 Effect of arc voltage on a bead weld



	CARBON STEEL ELECTRODE - A675			TYPE 310 STAINLESS STEEL ELECTRODE - A310		
WELDING SPEED (IN./MIN.)	12	12	12	12	12	12
TYPE OF CURRENT	AC	DCRP	DCSP	AC	DCRP	DCSP
CURRENT (AMPERES)	300	300	300	295	295	300
VOLTAGE (VOLTS)	21	29	21	21	30	21
WIRE DEPOSITION (LBS./HR.)	9.3	9.6	10.1	12.3	10.2	11.5
DILUTION (%)	31	41	30	29	42	34
PENETRATION (INCH)	0.10	0.13	0.08	0.11	0.16	0.12



	CARBON STEEL ELECTRODE - A675			TYPE 310 STAINLESS STEEL ELECTRODE - A310		
WELDING SPEED (IN./MIN.)	24	24	24	24	24	24
TYPE OF CURRENT	AC	DCRP	DCSP	AC	DCRP	DCSP
CURRENT (AMPERES)	300	295	300	295	295	295
VOLTAGE (VOLTS)	21	28	21	21	28	21
WIRE DEPOSITION (LBS./HR.)	9.3	9.6	10.2	12.3	10.2	11.5
DILUTION (%)	38	41	34	22	47	32
PENETRATION (INCH)	0.08	0.11	0.06	0.06	0.12	0.10

NOTE: ELECTRODE DIAMETER: 1/16 INCH ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH.
AC AND STRAIGHT POLARITY DC ELECTRODES WERE COATED WITH AN ACTIVATING AGENT

Fig. 6 Effect of welding speed on a bead weld

welding current produced the following results:

1. The DCRP bead welds still had the greatest dilution and penetration.

2. The average increase in penetration for all the bead welds was approximately 90%.

3. The dilution was increased for all welds.

4. A pronounced papillary was introduced into all of the bead welds, particularly for the DCRP deposits.

5. The average increase of deposition rate was 50% which is greater than the percent increase of current because of the effect of electrical resistance heating in the electrode.

Arc Voltages

Figure 5 illustrates the effect of increasing the arc voltage, at constant current. The increase in voltage had the following effect:

1. The dilution and penetration were decreased.

2. The deposition rate was decreased.

3. The arcs became more flexible and could wander; this caused a displacement of the papillary in the DCRP bead welds.

Welding Speed

Figure 6 shows the effect of increasing the welding speed from 12 to 24 ipm while the other welding conditions are held constant. Doubling the welding speed brought about the following results:

1. The penetration was slightly decreased but the change in dilution was inconsistent.

2. The DCRP bead welds still produced the greatest penetration and dilution.

Length of Electrode Stick-out

Figure 7 shows the effect of increasing the electrode stick-out from $^3/_4$ to $1^1/_2$ in. This increase in length causes an appreciable amount of preheating of the electrode wire prior to its melting by virtue of the increased electrical resistance heating in the wire. The effects are summarized as follows:

1. The dilution and penetration were decreased for all but the DCRP carbon-steel bead weld. This anomaly has not been explained.

2. The wire deposition rate was increased by approximately 35%.

3. Poorer wetting and greater build-up occurred, particularly with the stainless steel bead welds.

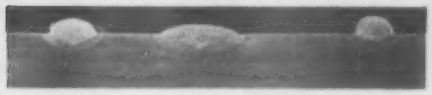
Type of Shielding Gas

Figure 8 shows the effect of varying the shielding gas while all of the other variables are kept constant. Examination of Fig. 8 indicates that the various gases influenced the bead weld as follows:

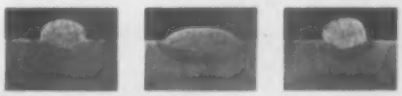
1. Argon produced the least dilution for both the carbon and stainless steel bead welds.

2. Helium produced greater dilution and penetration than argon.

3. Carbon dioxide produced the greatest deposition rate along with deep penetration and large dilution. It is recognized that carbon dioxide is not an inert gas and produces a great amount of oxidation but it was introduced here because of the recent interest in its usage.



	CARBON STEEL ELECTRODE - A675			TYPE 310 STAINLESS STEEL ELECTRODE - A310		
STICK-OUT (INCHES)	3/4	3/4	3/4	3/4	3/4	3/4
TYPE OF CURRENT	AC	DCRP	DCSP	AC	DCRP	DCSP
CURRENT (AMPERES)	300	300	300	295	295	300
VOLTAGE (VOLTS)	21	29	21	21	30	21
WIRE DEPOSITION (LBS./HR.)	9.3	9.6	10.1	12.3	10.2	11.5
DILUTION (%)	31	41	30	29	42	34
PENETRATION (INCH)	0.10	0.13	0.08	0.11	0.16	0.12

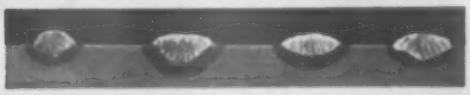


	CARBON STEEL ELECTRODE - A675			TYPE 310 STAINLESS STEEL ELECTRODE - A310		
STICK-OUT (INCHES)	1 1/2	1 1/2	1 1/2	1 1/2	1 1/2	1 1/2
TYPE OF CURRENT	AC	DCRP	DCSP	AC	DCRP	DCSP
CURRENT (AMPERES)	290	290	290	290	290	290
VOLTAGE (VOLTS)	23	32	22	24	30	22
WIRE DEPOSITION (LBS./HR.)	13.0	12.5	14.0	15.7	14.0	16.0
DILUTION (%)	22	41	20	20	31	16
PENETRATION (INCH)	0.07	0.13	0.07	0.10	0.14	0.09

NOTE: ELECTRODE DIAMETER: 1/16 INCH WELDING SPEED: 12 IN./MIN.
SHIELDING GAS: 99% ARGON + 1% OXIGEN @ 40 CFH.
AC AND STRAIGHT POLARITY DC ELECTRODES WERE COATED WITH AN ACTIVATING AGENT

Fig. 7 Effect of electrode stick-out on a bead weld

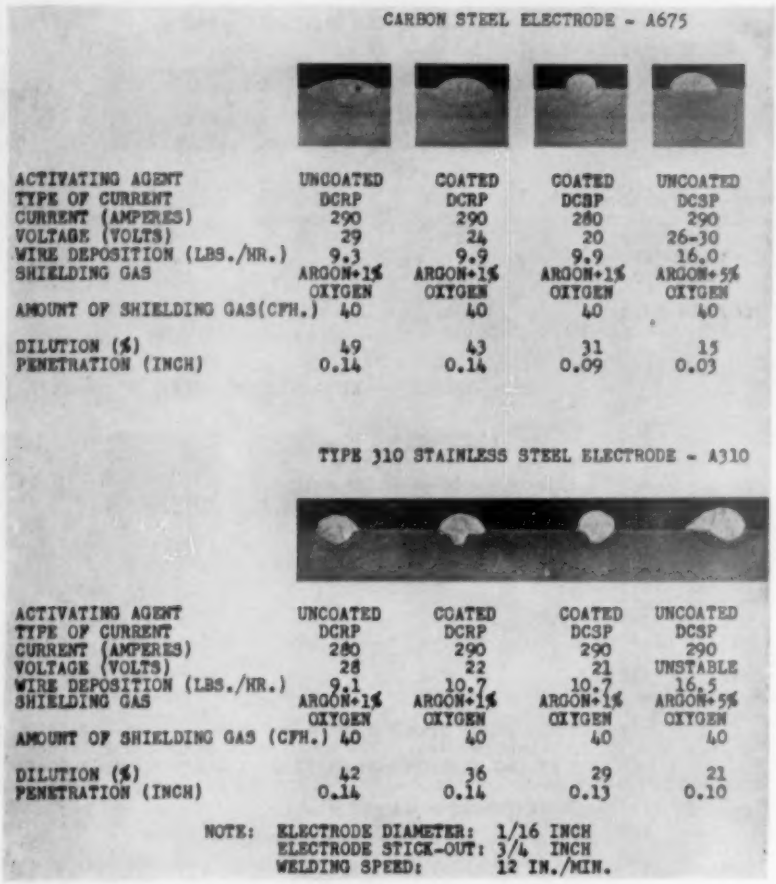
	CARBON STEEL ELECTRODE - A675			
SHIELDING GAS	ARGON	HELIUM	HE + AR	CO_2
SHIELDING GAS FLOW (C.F.H.)	40	80	45 15	40
TYPE OF CURRENT	DCRP	DCRP	DCRP	DCRP
CURRENT (AMPERES)	290	295	290	295
VOLTAGE (VOLTS)	27	44	37	32
WIRE DEPOSITION (LBS./HR.)	9.1	9.6	9.3	10.7
DILUTION (%)	43	71	50	65
PENETRATION (INCH)	0.14	0.15	0.08	0.19



	TYPE 310 STAINLESS STEEL ELECTRODE - A310			
SHIELDING GAS	ARGON	HELIUM	HE + AR	CO_2
SHIELDING GAS FLOW (C.F.H.)	40	80	45 15	40
TYPE OF CURRENT	DCRP	DCRP	DCRP	DCRP
CURRENT (AMPERES)	295	295	285	285
VOLTAGE (VOLTS)	27	45	36	29
WIRE DEPOSITION (LBS./HR.)	10.7	11.5	10.4	12.3
DILUTION (%)	37	52	51	53
PENETRATION (INCH)	0.13	0.20	0.12	0.16

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
WELDING SPEED: 12 IN./MIN.

Fig. 8 Effect of shielding gas on a bead weld



CARBON STEEL ELECTRODE - A675

ACTIVATING AGENT	UNCOATED	COATED	COATED	UNCOATED
TYPE OF CURRENT	DCRP	DCRP	DCSP	DCSP
CURRENT (AMPERES)	290	290	280	290
VOLTAGE (VOLTS)	29	24	20	26-30
WIRE DEPOSITION (LBS./HR.)	9.3	9.9	9.9	16.0
SHIELDING GAS	ARGON+1% OXYGEN	ARGON+1% OXYGEN	ARGON+1% OXYGEN	ARGON+5% OXYGEN
AMOUNT OF SHIELDING GAS(CFH.)	40	40	40	40
DILUTION (%)	49	43	31	15
PENETRATION (INCH)	0.14	0.14	0.09	0.03

TYPE 310 STAINLESS STEEL ELECTRODE - A310

ACTIVATING AGENT	UNCOATED	COATED	COATED	UNCOATED
TYPE OF CURRENT	DCRP	DCRP	DCSP	DCSP
CURRENT (AMPERES)	280	290	290	290
VOLTAGE (VOLTS)	28	22	21	UNSTABLE
WIRE DEPOSITION (LBS./HR.)	9.1	10.7	10.7	16.5
SHIELDING GAS	ARGON+1% OXYGEN	ARGON+1% OXYGEN	ARGON+1% OXYGEN	ARGON+5% OXYGEN
AMOUNT OF SHIELDING GAS (CFH.)	40	40	40	40
DILUTION (%)	42	36	29	21
PENETRATION (INCH)	0.14	0.14	0.13	0.10

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
WELDING SPEED: 12 IN./MIN.

Fig. 9 Effect of activating agents on bead welds made with direct current

Activating Agents in Direct-Current Welding

Figure 9 was prepared to show the effect produced on the appearance of bead welds when an activated wire was employed with either reverse-polarity or straight-polarity direct current. The activating agent stabilized the DCSP welding arc and produced good metal transfer. In addition, several other effects of the activating agent on DCRP and DCSP welding were noted as follows:

1. The dilution in DCRP welding was reduced by decreasing the heat liberated at the plate.

2. The deposition rate in DCSP welding was reduced which in turn increased the dilution.

3. The arc voltage for either DCRP or DCSP was reduced.

Multiple-Arc Overlays

Multiple-arc techniques with the consumable-electrode inert-arc process were developed to obtain maximum deposition rates with minimum dilution and penetration. These techniques incorporated two other control factors, auxiliary filler wires and oscillation, that influence the final quality of a welded overlay. Auxiliary wires were added to each welding arc to increase the deposition rate and reduce the penetration. The welding arcs and auxiliary wires were oscillated as a unit in order to obtain proper fusion of the overlay material with minimum penetration.

The a-c series arc technique proved to be the most satisfactory multiple-arc method of depositing materials stabilized for alternating-current welding by the addition of an activating agent. Overlays of carbon steel, Type 310 stainless steel, nickel-manganese steel and Type 61 nickel were made with the a-c series arc technique using the setup of welding equipment shown in Fig. 2. The two arcs were series connected across an a-c power supply. For sufficient voltage to start and maintain two arcs in series, two a-c welding transformers, having an open-circuit voltage of 75 v each, were connected in series to obtain a power supply with a total of 150 v on open circuit. An open-circuit voltage as low as 110 v could have been used, but none was available at the time of test. Each of the two leads from the combined power supply was connected through a contactor to an automatic welding head; no electrical connection was made to the workpiece. The workpiece became a part of the electrical circuit only when the welding arcs were established. A schematic circuit diagram for the a-c series arc technique is shown in Fig. 10.

The overlays made with this technique are shown in Figs. 11*A* and 11*B*. Single-arc overlays were deposited

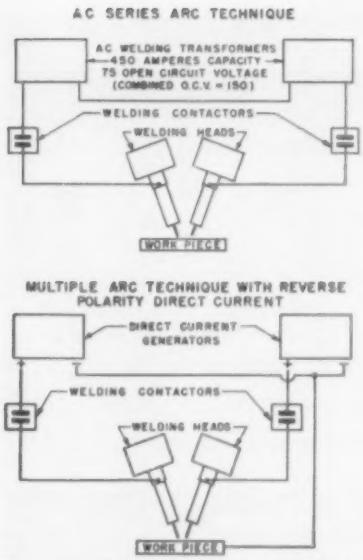


Fig. 10 Circuit diagrams for multiple arc techniques

beside the double-width multiple-arc overlays for comparison. When carbon steel, Type 310 stainless steel, nickel-manganese steel and Type 61 nickel overlays were made with the a-c series arc technique using auxiliary wires and oscillation, the improvements obtained over single-arc deposits were as follows:

1. Reduced dilution.
2. Decreased penetration.
3. Increased deposition rate.

Multiple-arc overlays were made with copper and aluminum bronze wires. Two reverse-polarity direct current arcs were employed for these electrode wires. Since low penetration could be obtained for copper or copper-base electrode wires with reverse-polarity direct current, activated electrode wires which are required for straight-polarity direct current or alternating current welding were not necessary. The lower melting point, higher burn-off rate and rapid freezing of the weld metal obtained with copper, probably made it possible to deposit a usable overlay with two reverse polarity copper arcs whereas unsatisfactory results were obtained with steel electrode wires. Auxiliary wires were added to the deoxidized-copper multiple-arc overlays. Auxiliary filler wires could not be added to the alumi-

CARBON STEEL ELECTRODE - A675

TYPE OF CURRENT	AC SERIES ARCS	AC
CURRENT (AMPERES)	305	310
VOLTAGE (VOLTS)	21	24
WELDING SPEED (IN./MIN.)	4.8	4.8
OSCILLATION (CYCLES/MIN.)	87	---
WIRE DEPOSITION (LBS./HR.)	26.4	9.8
DILUTION (%)	13	24
PENETRATION (INCH)	0.06	0.12

TYPE 310 STAINLESS STEEL ELECTRODE - A310

TYPE OF CURRENT	AC SERIES ARCS	AC
CURRENT (AMPERES)	320	320
VOLTAGE (VOLTS)	22	23
WELDING SPEED (IN./MIN.)	9	9
OSCILLATION (CYCLES/MIN.)	87	---
WIRE DEPOSITION (LBS./HR.)	30.8	10.7
DILUTION (%)	16	22
PENETRATION (INCH)	0.06	0.11

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH. IN EACH BARREL.
TWO 1/16 INCH DIAMETER AUXILIARY WIRES WERE ADDED TO THE AC SERIES ARCS.

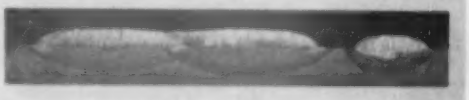
Fig. 11A Comparison of multiple- and single-arc overlays

Fig. 11B Comparison of multiple- and single-arc overlays

NICKEL MANGANESE ELECTRODE - A683

TYPE OF CURRENT	AC SERIES ARCS	AC
CURRENT (AMPERES)	270	310
VOLTAGE (VOLTS)	24	25
WELDING SPEED (IN./MIN.)	3.5	3.5
OSCILLATION (CYCLES/MIN.)	83	---
WIRE DEPOSITION (LBS./HR.)	28.5	11.2
DILUTION (%)	12	18
PENETRATION (INCH)	0.06	0.10

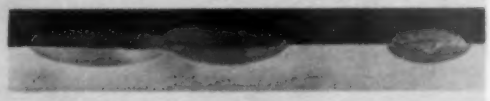
TYPE 61 NICKEL OVERLAYS - A261



TYPE OF CURRENT	AC SERIES ARCS	DCSP
CURRENT (AMPERES)	310	295
VOLTAGE (VOLTS)	22	26
WELDING SPEED (IN./MIN.)	5.8	5.8
OSCILLATION (CYCLES/MIN.)	87	---
WIRE DEPOSITION (LBS./HR.)	30.1	10.3
DILUTION (%)	13	42
PENETRATION (INCH)	0.08	0.11

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: 99% ARGON + 1% OXYGEN @ 40 CFH. IN EACH BARREL
TWO 1/16 INCH DIAMETER AUXILIARY WIRES WERE ADDED TO THE AC SERIES ARCS.

DEOXIDIZED COPPER OVERLAYS - A145



TYPE OF CURRENT (1)	TWO DCRP ARCS	DCRP
CURRENT (AMPERES)	290	300
VOLTAGE (VOLTS)	25	28
WELDING SPEED (IN./MIN.)	10	10
OSCILLATION (CYCLES/MIN.)	87	--
WIRE DEPOSITION (LBS./HR.)	38.4	15
DILUTION (%)	13	32
PENETRATION (INCH)	0.04	0.12

(1) TWO 1/16 INCH DIAMETER AUXILIARY WIRES WERE ADDED TO THE MULTIPLE ARC OVERLAY.

ALUMINUM BRONZE OVERLAYS - A158

TYPE OF CURRENT	TWO DCRP ARCS	DCRP
CURRENT (AMPERES)	285	285
VOLTAGE (VOLTS)	26	27
WELDING SPEED (IN./MIN.)	12	12
OSCILLATION (CYCLES/MIN.)	87	--
WIRE DEPOSITION (LBS./HR.)	22.6	10.5
DILUTION (%)	14	23
PENETRATION (INCH)	0.03	0.06

NOTE: ELECTRODE DIAMETER: 1/16 INCH
ELECTRODE STICK-OUT: 3/4 INCH
SHIELDING GAS: ARGON @ 40 CFH. IN EACH BARREL

Fig. 12 Comparison of multiple- and single-arc overlays

num-bronze multiple-arc overlays as uniform melting of the auxiliary wires could not be obtained. A schematic diagram for this welding method is shown in the lower half of Fig. 10.

The overlays obtained are shown in Fig. 12. Single-arc bead welds with DCRP were deposited adjacent to the double-width multiple-arc overlays for comparison. When the multiple-arc overlays were compared to the single-arc bead welds, the former were seen to have the following advantages:

1. Less dilution.
2. Less penetration.
3. Greater deposition rate.

Even though the operation of multiple arcs is more complex than single-arc operation, the advantages obtained with multiple-arc techniques should be given full consideration, particularly when large areas are to be surfaced with minimum dilution.

Conclusions

Minimum dilution and penetration in conjunction with high deposition rates are the normal requirements for all types of surfacing applications. Numerous factors influence these requirements with inert-gas-shielded metal-arc welding using a consumable electrode. The most important variables are type and amount of welding current, arc voltage, type of shielding gas, length of electrode stick-out, welding speed, the use of activated electrode wire, the use of more than one welding arc, the addition of an auxiliary wire to each arc and oscillation of the welding unit.

To obtain minimum dilution and penetration with consumable-electrode inert-gas welding with a single arc, the following process variables are recommended:

1. Use AC or DCSP current, activated electrode wire and argon shielding gas.
2. Use minimum welding current.
3. Use maximum arc voltage, welding speed and electrode stick-out.

To obtain maximum deposition rates with the lowest possible dilution and penetration, multiple-arc techniques incorporating the following conditions are recommended:

1. For the carbon, high-alloy steels, and nickel alloys, the AC series arc technique should be used. For copper and copper base alloys DCRP parallel arcs can be used.
2. Auxiliary filler wires for each arc should be employed.
3. Oscillation of the arcs and auxiliary wires is essential.

DESIGN AND APPLICATION OF EDGE-RING PROJECTION WELDING

BY CHESTER A. CZOHARA

Resistance welding process provides a more feasible method of manufacturing rear spring mounting bracket for light-duty model trucks

Chester A. Czohara is Welding Engineer, Manufacturing Research Dept., International Harvester Company, Chicago, Ill.

This paper won the 1953 Industrial Award offered by Resistance Welder Manufacturers' Association.

Introduction

The resistance welding process was considered for joint redesign of a rear spring mounting bracket which is used on the light duty model International Harvester Trucks. The spring bracket is a part of the chassis assembly, being riveted to the side rails of the frame.

The original concept of this particular model of spring bracket was designed for hydrogen furnace brazing. The furnace brazing process of manufacturing demands close tolerances on all component parts. The initial cost of the required equipment such as the brazing furnace, controls, etc., was found to be very costly for the work load involved. It was, therefore, decided to

Fig. 1 Series projection welding of spring bracket in production

most important variables are type and amount of essential.

fabricate the spring bracket by the electric arc welding process, using the same general design with a few modifications.

Due to various factors, the manufacture of the spring bracket was sublet to an outside concern. The quality of workmanship obtained gradually decreased with time, therefore increasing the number of field failures encountered. The quality of welding had to be maintained in order to correct this condition which resulted in the necessity for paying a premium price for the spring mounting bracket.

Consideration of Joint

In order to determine a more feasible method of manufacturing the spring bracket, an evaluation was made of the production arc welded joint design.

Fig. 2 Typical field failure, arc-welded design, rear spring mounting bracket

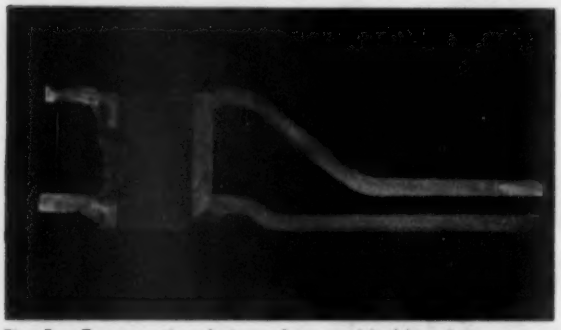


Fig. 3 Cross-sectional view of arc-welded bracket

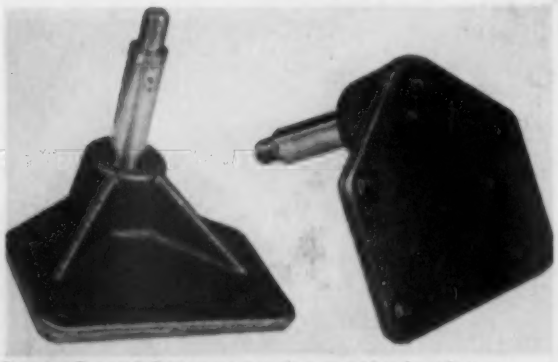


Fig. 4 Forged design rear spring mounting bracket

A typical field failure of an arc welded spring bracket is shown in Fig. 2. The poor quality of workmanship is evident. A cross-sectional view of the same design arc welded joint (Fig. 3) discloses porosity in the plug weld. Excessive welding current resulted in undercutting and burning away at the junction of the bushing and plate stampings.

A forged design spring bracket was also considered in this investigation (See Fig. 4). This design had only two component parts, namely, a forged mounting plate and a pin. The pin was pressed into the mounting plate and tack welded on the back side. Laboratory and field records showed that the fatigue life of the forged design spring bracket was short, that a point of high stress concentration was located at the junction of the pin and plate causing premature fracturing of the pin; this was due to the rigidity of the forged mounting plate.

The observations made from a study of the various designs and the results of both laboratory and field tests made it evident that a 360° welded joint was necessary for attaching the pin to the mounting plates. This was a feature which neither the arc welded or the forged design contained.

The general features of the arc welded joint used in the spring bracket was maintained in redesigning the joint for resistance welding. The front and back plate stampings (*c* and *a*) were utilized as shown in Fig. 5. The only change in these stampings found necessary was to reduce the diameter of the pin hole. The two projection embossments in the front plate (*c*) were eliminated in lieu of two spot welds which were made in the final assembly of the bracket. The diameter of the pin (*b*) Fig. 5 in the new design was increased, eliminating bushing (*b*) in the old design. The shank diameter of the new pin design is turned down and ground to size within the same limits as in the old pin.

In Fig. 6 is shown the final assembly of the resistance welded spring bracket (right) and the old design arc welded spring bracket (left). It should be noted that the resistance welded bracket is a clean, spatter (metal) free, smooth appearing unit.

Development of the Resistance Welded Joint Design

In order to establish a 360° welded joint about the pin, a ring type projection design was considered as being the most feasible. The diameter of the pin (*d*-left) Fig. 5 of the old design, was modified by increasing the diameter on the end of the pin. The shank diameter of the pin remained the same. The increased diameter of the new designed pin (*b*-right) provided for two conditions, namely: (1) a chamfer or edge-ring projection on both ends, that is, a chamfer on the front and back at the shank end and (2) a shoulder to limit the amount of set-down during the welding operation. The new pin design lends itself very readily to a screw machine operation. The threaded diameter, the shank diameter and the two (2) chamfers or ring projections require

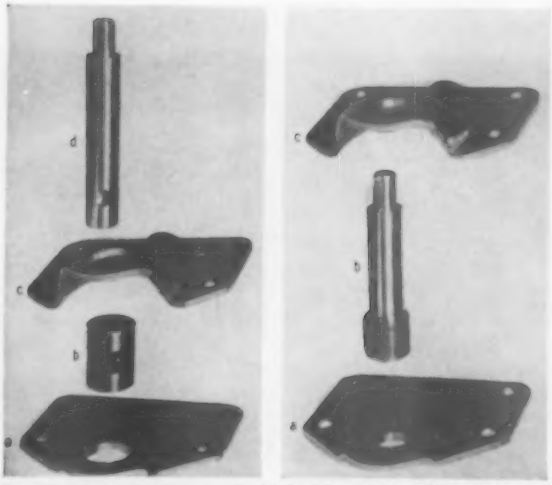


Fig. 5 Exploded view of old *(left)* and new *(right)* design spring bracket components

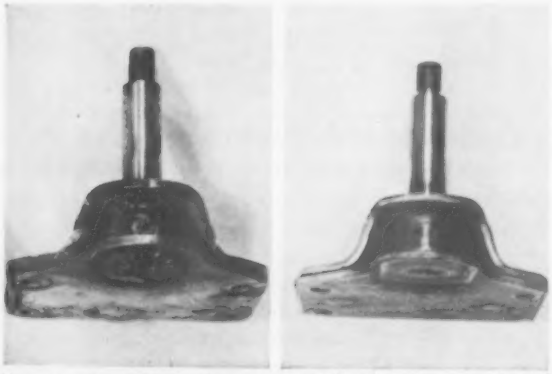


Fig. 6 Assembled view of old *(left)* and new *(right)* design of spring bracket

close tolerances in concentricity to each other. The outside diameter of the pin is mill stock (cold rolled), which does not require machining or holding to close concentric tolerances.

This peculiar design and location of the two edge-ring projections made it necessary to weld them simultaneously. As can be seen, this resulted in making two projection welds in series, a condition which is generally considered to be a poor resistance welding practice. However, future work along this line as well as production experience has indicated that this design and method of welding is very practical.

The size of chamfer to form the edge-ring projection was based on several factors, namely: (1) No metal expulsion was permissible at the junction of the pin shank diameter and the front plate (*c*-right) Fig. 5. Any expelled metal adhering to the front plate would cut into the rubber grommet which is placed over the pin in the final truck chassis assembly; (2) A full depth weld ring nugget is required, which would be equal to the thickness of the plate stampings (0.1793 in. thick) (see Figs. 13 and 14 of push test specimen). The weld strength must exceed that of the parent metal.

The initial design of the edge-ring projection was $^{3}/_{32}$ in. x 60° angle. The diameter of the holes in the plate stampings were made large enough to locate the projection, prior to welding, approximately 50% into the hole, thereby providing a means of locating the pin. This method of centering the pin, however, was found to be unsuccessful. In locating the projection partially into the hole, only a fraction of the chamfer is actually fused to the plates forming the ring (weld) nugget. The size of the ring (weld) nugget varied, depending upon the hole size in the plate stampings. In other words, if the diameter of the hole in the plate stamping was on the minus side (within specified tolerance), the ring nugget would be larger. The ring nugget becomes smaller if the diameter of the hole in the plate was on the plus side. In either case, the welding sequence would be the same but the set down would be inconsistent.

Numerous combinations of edge-ring projections were investigated, ranging from $^{3}/_{32}$ to $^{5}/_{32}$ in. high with angles of 60 and 45°. Through extensive experimental work, a final design was established as being $^{9}/_{64}$ in. x 45°. The maximum projection height which could be used was limited by the thickness of the plate material for the stamping (0.1793 in.). It was found necessary to establish tolerances between the root diameter of the ring projection and the diameter of the hole in the plate stampings in order to consistently obtain the maximum ring nugget. The optimum clearance was found to be between 0.001 to 0.007 in. on the diameter. Holding this clearance also made it possible to make full depth (plate thickness) welds and not obtain metal expulsion on the front plate, which met both of the requirements previously outlined.

Welding Procedure

The preliminary projection and spot-welding investigation was carried out in the laboratory on a 250 kva press type welder, having a pneumatic operated head. A Westinghouse sequence weld timer, type NEMA 5B with special forge delay control was used. Current measurements were taken with a General Electric pointer stop a-c ammeter connected to a split core 500 to 1 current transformer attached to the 440 volt primary line. The electrode pressure was obtained by using a General Electric surface pressure gage.

In actual plant operations, modifications had to be made from the laboratory setups to suit production requirements and operating personnel. An excellent example of the production setup in projection welding the spring bracket is shown in Fig. 1. In order to facilitate loading the parts in the projection welder and aligning the 4 rivet holes in both plate stampings, the components are projection welded in an upside down position. This is illustrated in Fig. 7 which shows a close-up view of the component parts on the welder ready to be projection welded. An automatic air operated ejector system is used for removing the part after projection

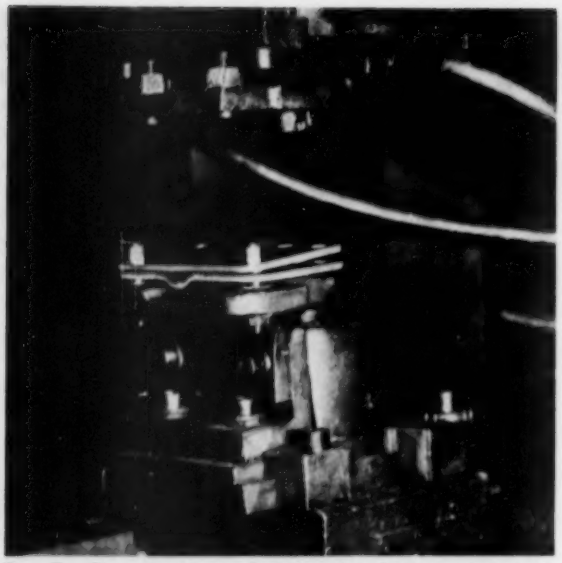


Fig. 7 Close-up view of spring bracket components in welding dies shown in Fig. 1

Fig. 8 Close-up view of spring bracket being ejected after welding

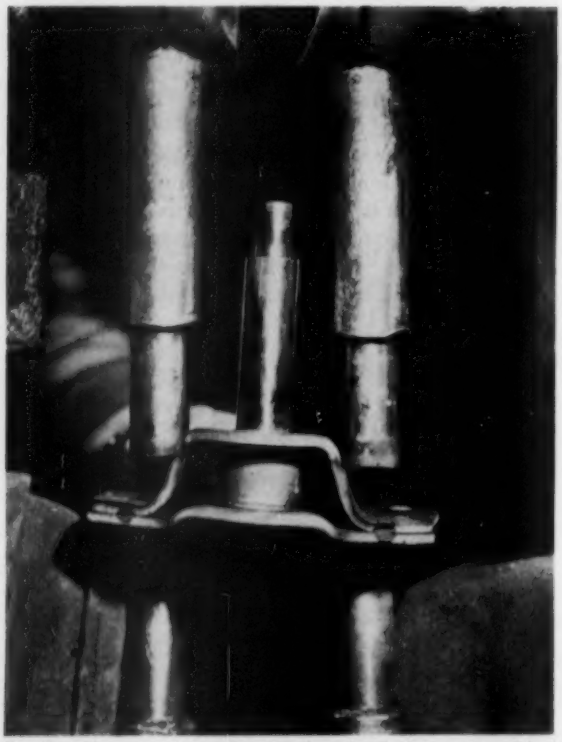


Fig. 9 Close-up view of spring bracket being spot welded in production. Two spot welders are being used

welding. Fig. 8 shows a spring bracket being ejected after welding. This automatic power ejection is necessary due to the confining nature of the dies and the close clearances necessary to hold the pin in correct alignment.

The production spot welding operation is done on two 150 kva spot welders, facing each other so that both spot welds are made simultaneously as shown in Fig. 9. The welders were utilized to speed up the spot-welding operation.

The design of the lower projection welding die is a prime factor in producing consistent results once the product design is established. The die must contain three important features: (1) a means for locating the pin accurately and hold the alignment during welding; (2) a means for locating and aligning the two plate stampings; (3) a means for conducting the welding current to the projection weld area, made of material which will stand up under production conditions. The ultimate design of the die is made up of seven component parts (Fig. 10). The copper water jacket (4) contains internal spiral grooves used to circulate water around the copper die body (5). A water tight shrink fit secures this jacket to the copper die body. The die insert (6) is made of R.W.M.A. Class 2 copper alloy, set into the die body (5). The insert is readily replaced without removing the entire lower die from the welder by removing the retainer washer (7). An average of 1150 assemblies are projection welded before it is necessary to change the inserts. The insert is always replaced, never being dressed while in the die. The front plate stamping contains an indentation which provides a pre-location means on the insert (6). The shank of the pin is concentrically located by the insert (6) and by a brass wear bushing (2). The brass wear bushing (2) is insulated by a Textolite insulation sleeve (1) thus eliminating the possibility of current shunting during welding. The front and back plate stampings are located through the rivet holes by two steel pins which are insulated from the locator. The locator, containing the steel pins, incorporates a means whereby

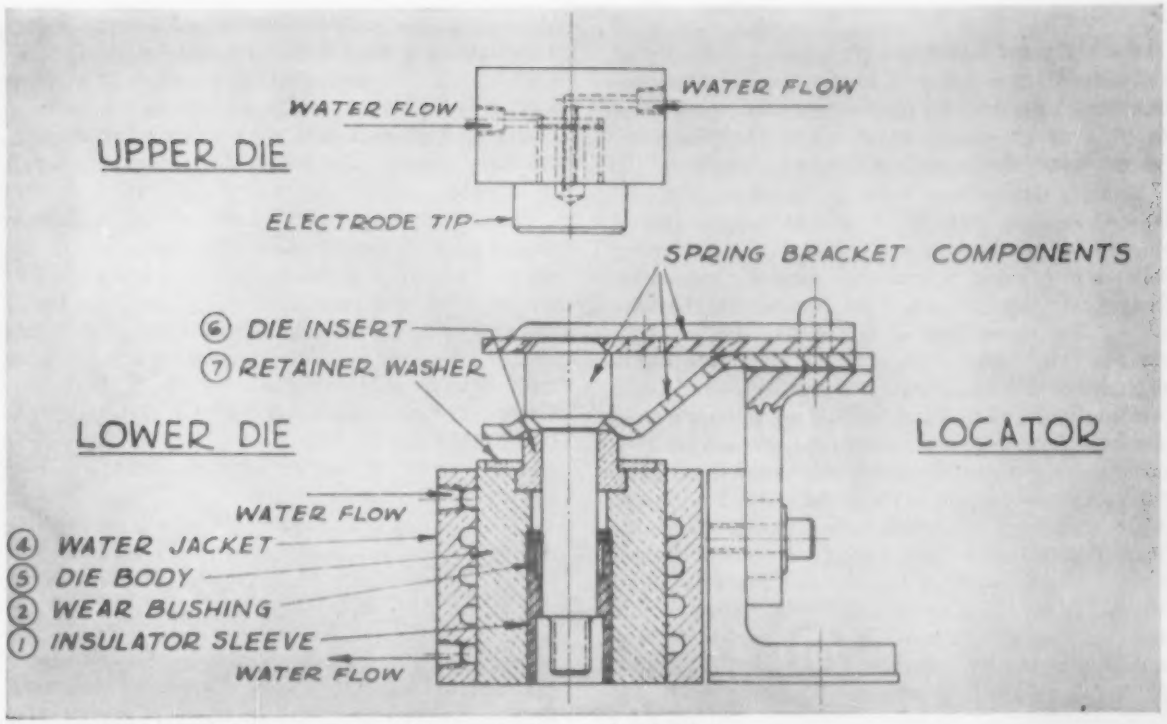


Fig. 10 Projection welding die arrangement for rear spring mounting bracket

horizontal and vertical adjustments can be made to obtain proper alignment between the pin and the four rivet holes in the plate stampings.

The upper projection welding die has a special screw type, water-cooled electrode tip made of R.W.M.A. class 2 copper alloy. The electrode tip is dressed in place after each 2500 assemblies and replaced after 12,500 assemblies.

Two separate welding operations are required to assemble the spring bracket. Welding data concerning these two operations is as follows:

Operation No. 1

Description:
Projection weld pin to front and back plate stampings.

Welding Schedule:

Electrode force between dies	3,800 lb
Squeeze time	120 cycles
Weld time	80 cycles
Cool time	300 cycles
Postheat time	27 cycles
Primary current (measured)	1,900 amp
Secondary current (calculated)	64,000 amp

Welding Die: See Fig. 10

Operation No. 2

Description
Spot weld front plate to back plate.

Welding Schedule:

Electrode force between tips	2,500 lb
Squeeze time	120 cycles
Weld time	50 cycles
Hold time	30 cycles
Primary current (measured)	1,300 amp
Secondary current (calculated)	44,000 amp

Welding Tips:
Material—R.W.M.A. class 2 copper alloy
Tip Diameter — $^{7}/_{8}$ in. diameter, #3 Morse taper,
Tip Face—10 in. spherical radius

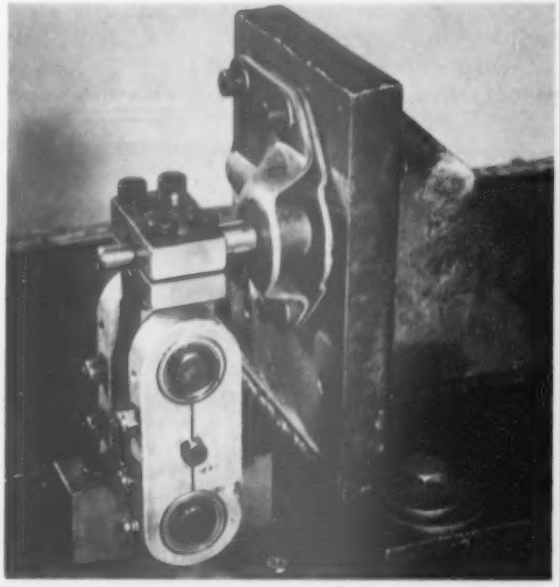


Fig. 11 Laboratory set up for fatigue tests of spring bracket

Method of Testing

The resistance welded spring brackets were subjected to bending fatigue and push tests in the laboratory before being submitted for further field track tests. Detailed examinations included magnaflux, hardness, microscopic, chemical and spectrographic studies.

Bending fatigue tests were conducted on a Model SF-1-U Sonntag 2000 lb, Universal fatigue testing machine using the fixture shown in Fig. 11. Typical failures in bending fatigue were generally caused by fatigue cracking of the pin at the junction with the front plate. This same type of failure was similar in all designs investigated, although the endurance limits extended over a longer period of time on the resistance welded design. Figs 3 and 12 show respectively a cross sectional view of an arc welded joint specimen and resistance welded joint specimen which exhibit a typical fatigue failure of the pin at the junction with the bracket plate stamping. All brackets had passed magnaflux inspection before fatigue testing. Extensive fatigue tests were not conducted on the spring brackets due to the lack of coordination between the laboratory fatigue tests and actual field conditions. The type of failure obtained in the laboratory (Figs. 3 and 12) were not the same as the type of failures experienced in the field (Fig. 2). The laboratory fatigue test was also unable to evaluate the joint designs under consideration as the same type of fracture occurred in the arc welded, forged and resistance welded designs, with only slight variations in the endurance limits.

Laboratory tests were conducted to find the force in pounds necessary to rupture the weld by a direct push on the pin, perpendicular to the weld area. Loads were applied with a 60,000 lb capacity Southwork-Tate-Emery Universal testing machine. A typical rupture is illustrated in Figs. 13 and 14. A series of push tests were conducted which indicated satisfactory consistent values. The results of these tests are shown in Table 1.

Photomicrographs were made of the resistance welded joint; a typical specimen is shown in Fig. 15. Micro-examination shows evidence of grain growth in the pin (C-1030) at the fusion line. The pin material had a structure of pearlite in a ferritic matrix. A Tukon hardness check of the pin at a point about 0.002 in. from the fusion line indicated a hardness of Rb 89-91 (converted). Micro-examination of the plate material (C-1010 rimmed steel) showed a structure composed almost entirely of ferrite with scattered grains of coarsely spheroidized pearlite.

The chemical and spectrographic analyses made of the pin material indicated that it was SAE C-1030.

One of a series of track field tests conducted was to

Table 1—Typical Values of Push Test of Resistance Weld Joint of Spring Bracket

Sample No.	*Front weld, lb*	*Back weld, lb*
1	23,550	23,300
2	22,600	19,750
3	19,950	21,350
4	23,150	18,350
5	22,200	19,400
6	19,950	20,200
7	21,150	22,750
8	23,800	21,700
9	24,300	22,050
10	23,650	20,000
11	22,800	20,650
12	22,850	20,150
13	25,750	23,500
14	23,200	22,550
15	22,650	25,150
16	22,550	26,400
17	22,350	23,800
18	21,800	18,250
19	21,700	22,400
20	20,750	23,000
21	24,200	22,200

Standard deviation for front weld strength ..... 3,614 lb
Standard deviation for back weld strength ..... 3,710 lb
Average strength for front weld ..... 22,471 lb
Average strength for back weld ..... 21,710 lb

Longitudinal cross sections were made of a resistance welded joint and a hardness survey was conducted, the results of which are shown in Table 2.

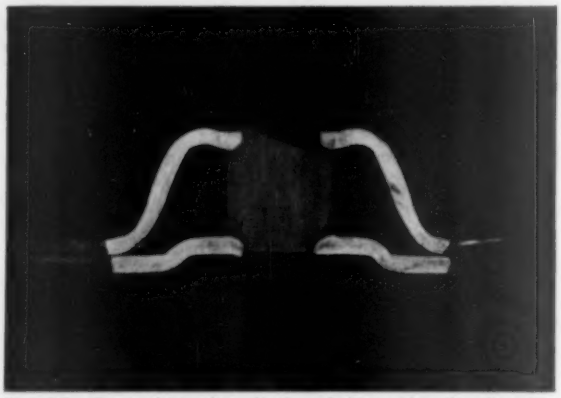
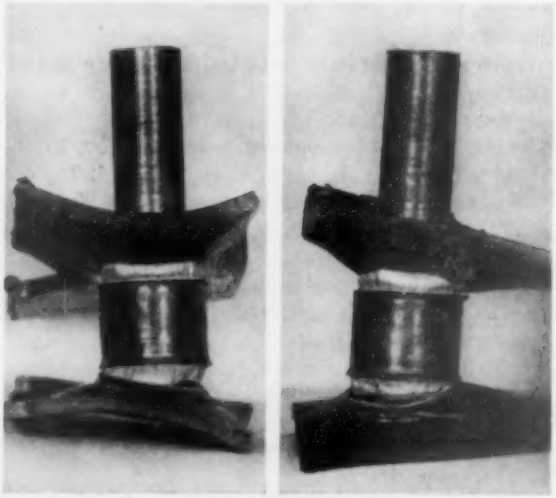


Fig. 12 Typical bending fatigue failure of spring bracket



Figs. 13 and 14 Laboratory test specimen of ring projection showing push test on top and bottom welds

subject a set of resistance welded rear spring brackets mounted on a test truck to the Belgian Block road test as shown in Fig. 16. In this Belgian Block road test, the component parts under examination, as well as the entire truck, are given extreme shock loads. This type of test is considered to be beyond the normal use of truck loads. Mileages accumulated in these accelerated tests may vary with the units and trucks under observation. However, if evidence of failure are observed in less than 250 miles, the design is generally considered inadequate and field complaints may be expected. Fig. 17 illustrates a resistance welded spring bracket which

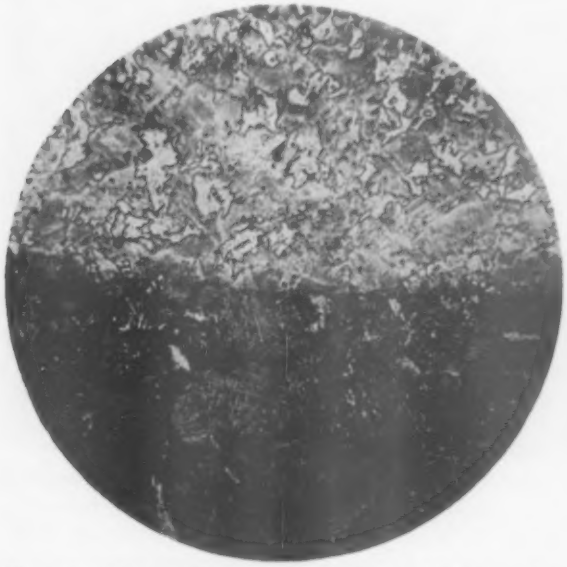
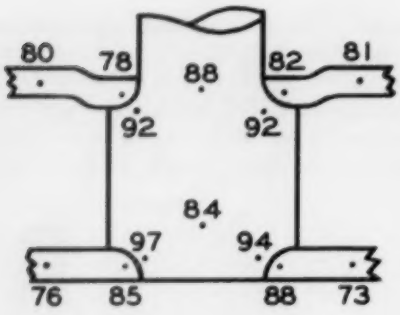


Fig. 15 Micrograph of ring projection weld from spring bracket showing structure of pin (C-1030) bottom and plate (C-1010) at fusion line. Magnification × 100. Etched 2% nital

Fig. 17 Resistance welded rear spring mounting bracket after being subjected to Belgian block road test

Fig. 16 Belgian block road test being conducted at the International Harvester test track



HARDNESS Rb
SCALE READING

Table 2—Hardness Survey of Edge Ring Projection Welds

was removed from a truck which underwent the Belgian Block test. It will be noted that the shank of the pin had been badly marred, showing evidence that the rubber grommets, which fit over the pin, were damaged.

Quality Control

Since the resistance welding process was introduced in the production of the rear spring mounting bracket, a systematic check, by destructive methods, was made over a period of twelve months. During this period it was found that the electrode pressure, welding current and welding heat time used varied as much as plus or minus 5% from the desired settings. It was observed that, even with these variations, complete fusion of edge-ring projection welded joints were obtained.

A quality control *p*-chart was set up for the resistance welding operation of the spring bracket. Samples from each hour's production were selected at random and quality tests were made. Results of these tests were plotted on the *p*-chart in the form of a fraction defective, that is, the ratio between the number of defectives found to the total pieces checked. After sufficient data had been collected, the average fraction defective *p* and control limits were computed and were drawn on the chart. Subsequent samples were selected, tested and it was found that the process was well within control. Due to the small number of defectives found, it was possible to reduce the sampling from once each hour to twice during an eight hour shift.

Table 3—Cost Comparison Between Arc Welded and Resistance Welded Rear Spring Mounting Bracket

	Manufacturing costs per 100 Assemblies		
Component or Operation	*Arc welded design*	*Resistance welded design*	*Cost reduction*
Back Plate (*a*)†	*	*	
Front Plate (*c*)	*	*	
Pin (*d*, left)	$13.92	..	
Pin (*b*, right)	..	$24.32	
Bushing (*b*, left)	$14.50	..	
Welding Operation	..	..	
Arc & Projection Weld	$17.24	..	
Projection & Spot Weld	..	4.05	
Total	$45.66	$28.37	$17.29

NOTE: *Not considered as the costs were not affected by the design changes.
† Small letters in parenthesis refer to Fig. 5.

Economy

Although the principal motivation behind changing the joint design to incorporate resistance welding was to improve quality, it soon became apparent that certain economies in manufacturing costs would be realized. A portion of the economy actually realized resulted in that fact that the former arc welded spring bracket was purchased completely ready for the assembly line whereas the new resistance welded design was completely fabricated within the company's facilities. In order to arrive at a fair economy figure which would reflect only those economies resulting from a change in the joint design and welding process, an estimate was made of the manufacturing costs of the arc welded design, assuming that it would be manufactured within the company's facilities, not purchased. So as to simplify these cost figures, only those items which were affected by the design change were considered. It was assumed that the manufacturing costs of the back and front plates (*a* and *c* in Fig. 5) would not be affected.

Table 3 contains a summary of the economy resulting from this investigation. It shows that a cost reduction of $17.29 per 100 assemblies was a direct result of the change in joint design to incorporate resistance welding rather than arc welding. It should be noted that the welding costs listed for both the arc welded and resistance welded design includes the preliminary work, such as the assembling of component parts, which is required preparatory to welding.

Conclusions

1. The adaptation of the resistance welding process to the rear spring mounting bracket retained and maintained a high quality product at a reduced manufacturing cost as compared to the former design using the arc welding process.

2. The number of components were reduced from four to three parts.

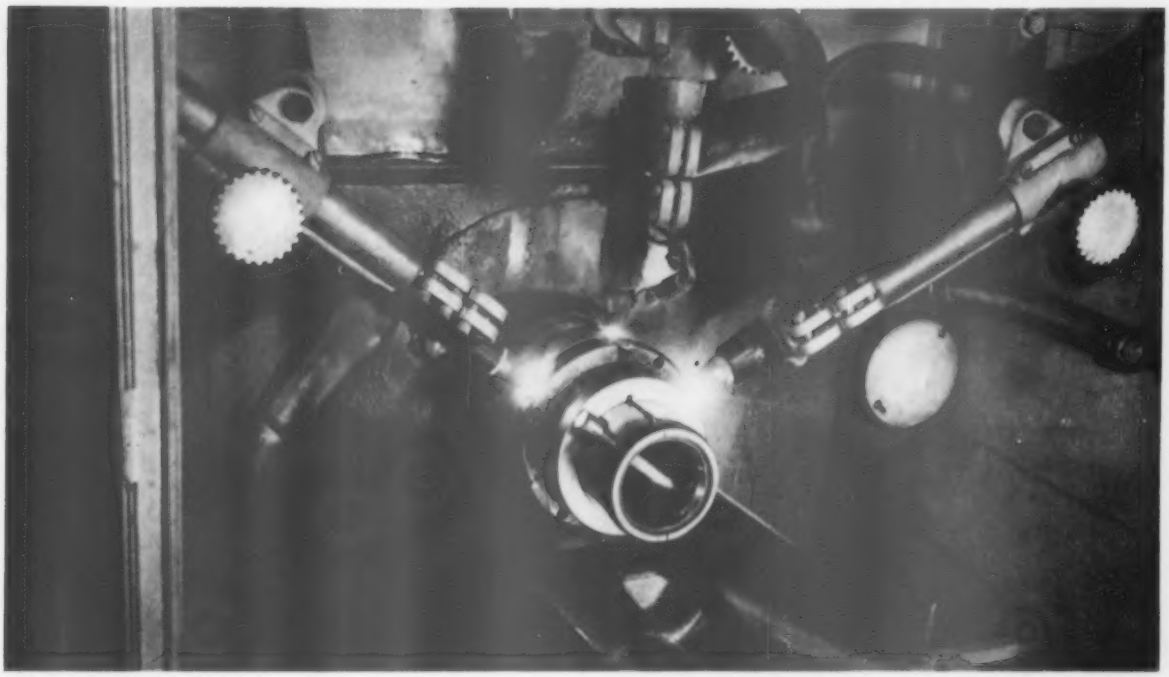
3. The design of the plate stampings was retained with slight modification in the pin hole size.

4. The projection embossments were eliminated in the front plate stamping in lieu of spot welds.

5. Due to the widened tolerances in the resistance welded design bracket, the ease of assembling the components reduced handling time, which was not permissible in the arc welded design bracket.

Acknowledgment

The author wishes to acknowledge the helpful suggestions offered by International Harvester Co. Manufacturing Research personnel, specifically J. E. Fitzwater, General Supervisor of Welding Research and R. C. Becker, Asst. General Supervisor of Welding Research. Also L. E. Drum, Works Manager and E. J. Tyree, Welding Engineer of our Springfield Works for their cooperation in providing field test data.



Inert-gas metal-arc welding of stator laminations

INERT GAS WELDING OF STATOR PACKS

Paper relates the requirements for welding, the factors influencing and controlling costs and the machine design suggestions for stator pack welding

BY F. J. PILIA

The electric motor has become an important item in industry and at home. The importance of the electric motor can be realized when you stop to think that almost everything we do is, in some way, influenced by an electric motor.

The motor industry is constantly striving to produce superior motors at reduced manufacturing costs. New methods of manufacturing and new types of motors are being designed to bring this about. The inert-gas welding process developments are keeping pace with the motor manufacturers, by the development and improvement of methods for applying these processes to the manufacture of electric motors.

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This paper will cover the application of the inert-gas-welding processes to the manufacture of electric motor stators.

Welding, in one form or another, has been used by the electrical manufacturing industry for many years. The flux-coated metal-arc process was used as a method of welding the edges of the stator laminations together to form a solid assembly. The flux-coated metal-arc process, however, produces an appreciable amount of spatter which adheres to the outside of the stator. Often flux is deposited between the laminations. This prevents the formation of oxides in the subsequent furnace operations, and results in a reduction in motor efficiency.

The carbon-arc process has been employed to weld the

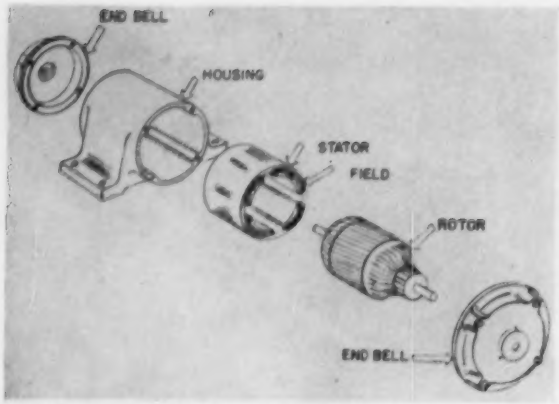


Fig. 1 An exploded view of a typical electric motor

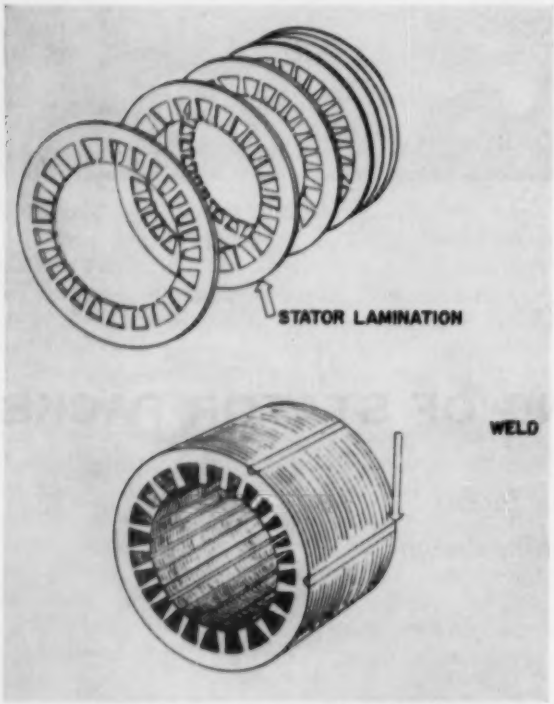


Fig. 2 A typical lamination from a fractional horsepower squirrel-cage induction motor

laminations. However, it is of limited usefulness because carbon introduced into the weld puddle contributes to transverse weld cracking, and occasionally large blow holes between the laminations.

Submerged arc welding also has been used to weld laminations, particularly on very large horsepower motors.

Inert-gas metal-arc welding with a relatively nonconsumable tungsten electrode has been applied to the welding of stator laminations since 1946 and is the most successful of all the welding processes used to date. This process produces no spatter and requires no buildup above the surface of the stator. It provides excellent penetration and is used without fluxing materials to contaminate the lamination adjacent to the weld zone. An inert-arc welded stator produces a minimum of loss electrically, and provides an excellent stator with regard to mechanical stability. The cost of manufacturing a stator by inert arc welding is usually less than riveting or other mechanical methods of fabrication.

Figure 1 is an exploded view of a typical electric motor. The electromagnetic elements are the stator and the rotor. The stator is mounted in a housing supported by a base, and the rotor is supported by two end bells which are attached to the housing. The stator on a modern a-c electric motor is made of laminated sheet steel.

Figure 2 illustrates the appearance of a typical lamination from a fractional horsepower squirrel-cage induction motor. The lower assembly shows the laminations welded together to form the finished stator.

Process Selection

Inert-gas metal-arc welding with a nonconsumable electrode and concentrated high heat input is ideally suited for fusing electrical laminations together. The process lends itself readily to automatic operations and requires a minimum of control apparatus for proper process operations.

Inert-gas metal-arc welding with a consumable electrode can be used on stators which require the addition of filler metal to the weld. Grooves of some form are usually required when this process is used.

The process selection for the inert-gas stator-pack welding is usually determined by the chemical composition of the steel used for the laminations.

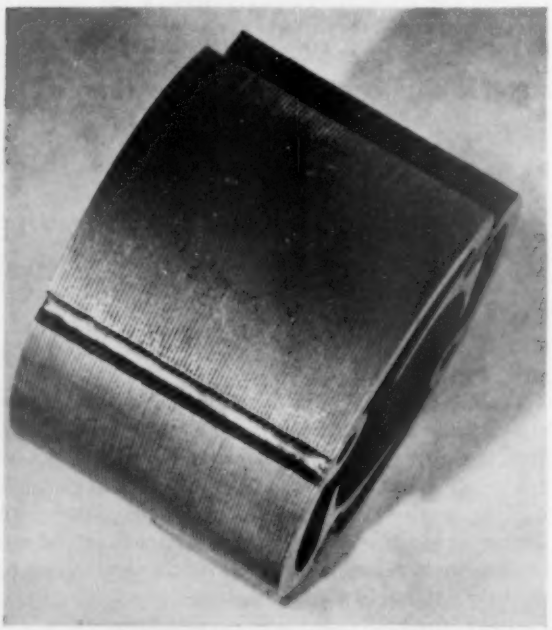


Fig. 3 A weld bead made on stator laminations that have been treated with a light-bodied type of coreplate

Effect of Steel Composition

Silicon is the principal element added to the electrical steels to give it special electrical characteristics. In the manufacture of steel, silicon is used as a deoxidizer. Small amounts, up to $^1/_2\%$, are used for this purpose. The addition of silicon to electrical steels above the amount required for deoxidation induces grain growth. The grain size has a controlling influence on the hysteresis loss in a motor, and to some degree on the eddy current losses. The larger the grain size in the lamination, the more efficient the motor. The high silicon content, however, has an adverse effect on the ductility of a weld in the stator laminations. Brittle fracture occurs in the weld at room temperature when the silicon content exceeds $2^1/_2\%$. Steels containing silicon in excess of $2^1/_2\%$ usually require the addition of a low-carbon, low-silicon filler wire to prevent cracking. Inert arc welding with nonconsumable electrode is more economical than the inert arc process with consumable electrode and should be used on stators wherever possible. However, the silicon content of the steel is the factor which determines the process to be used.

Effect of Surface Treatment of Steel

The surface condition of the electrical steel is second in importance to the composition, because it has a pronounced effect on the welding costs and speeds. Four types of surface condition are generally present on electrical grade steels. They are:

1. A cold-rolled surface.
2. An oxide-coated surface.
3. A coreplated surface.
4. A baked coreplate surface.

A clean cold-rolled surface and an oxide-coated surface does not interfere with the application of the inert-arc nonconsumable-electrode welding process. It is quite possible that, in some cases, the presence of an oxide is somewhat beneficial.

Coreplate coatings (Type 3 above) are in general use today. Some of the coreplates do not necessarily affect the welding operation. Figure 3 illustrates the appearance of a weld bead made on stator laminations that have been treated with a light-bodied type of coreplate.

Other coreplates require reduced welding speeds or cannot be welded without causing excessive porosity as shown in Fig. 4. The appearance of the weld bead made on laminations of this type, coated with the heavy black varnish coreplate, exhibits excessive porosity in the weld. Weld failure is likely to occur in the affected area.

Fig. 4 Excessive porosity caused by heavy black varnish coreplate

A fourth type of surface (Type 4 above) used by the motor industry is a heavy black varnish coreplate which is passed through a baking operation. The baking operation reduces the varnish coating to a very hard tenacious surface. This surface produces excessive porosity with both the nonconsumable- and consumable-electrode welding processes, and is generally not recommended for inert gas welding.

Some manufacturers have found that if coreplate is a problem, it is more economical to change the type of coreplate or resort to an oxide surface in order to use the inert-gas-welding processes to their best advantage.

Dimension Control

The application of the inert-gas-welding processes to the manufacture of electric motor stators requires the control of several important dimensional factors such as are shown in Fig. 5. The bore diameter of a stator is the most critical dimension in the stator since it is the most difficult to correct after the assembly is complete. It is important that the end of the stator be square with the bore of the stator for proper location of other motor parts. Proper compression of the lamination must be maintained during welding in order to develop the maximum efficiency of the motor and the rigidity of the stator. The problem of interlamination separation, stator deflection or distortion, and square ends are more difficult to control with riveted or bolted types of construction than with a welded type of a stator.

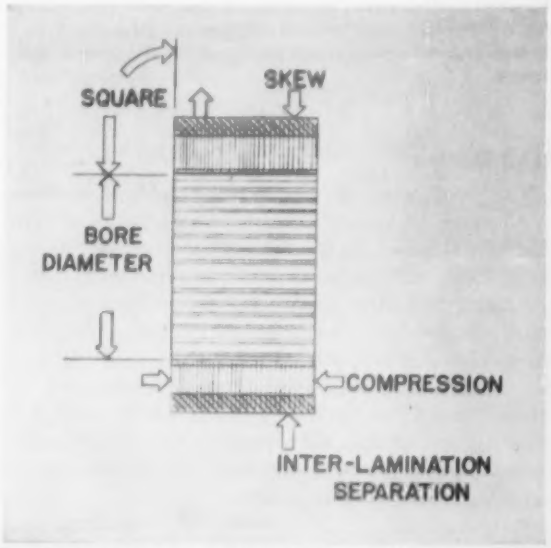


Fig. 5 Important dimensional factors in the proper alignment of stators

buildup above the surface of the stator. It provides

been treated with a light-bodied type of coreplate

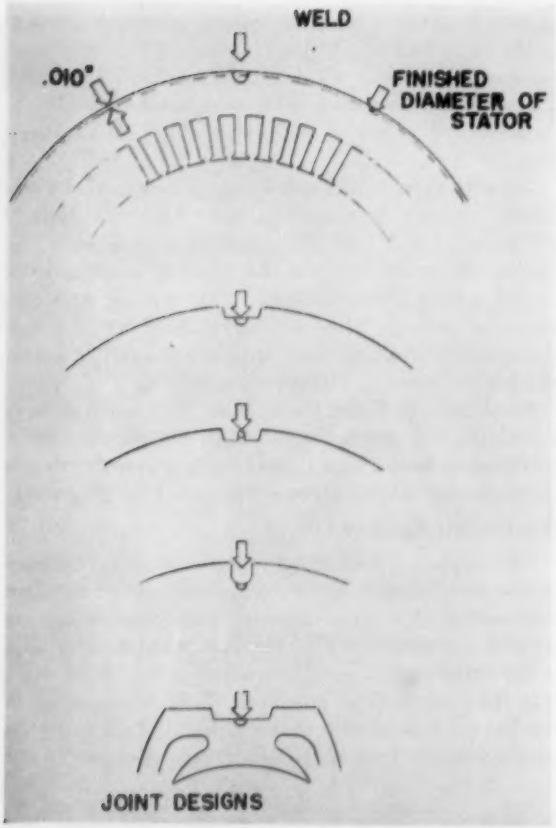


Fig. 6 Several popular joint designs currently being used for nonconsumable-electrode inert-gas metal-arc welding of stators

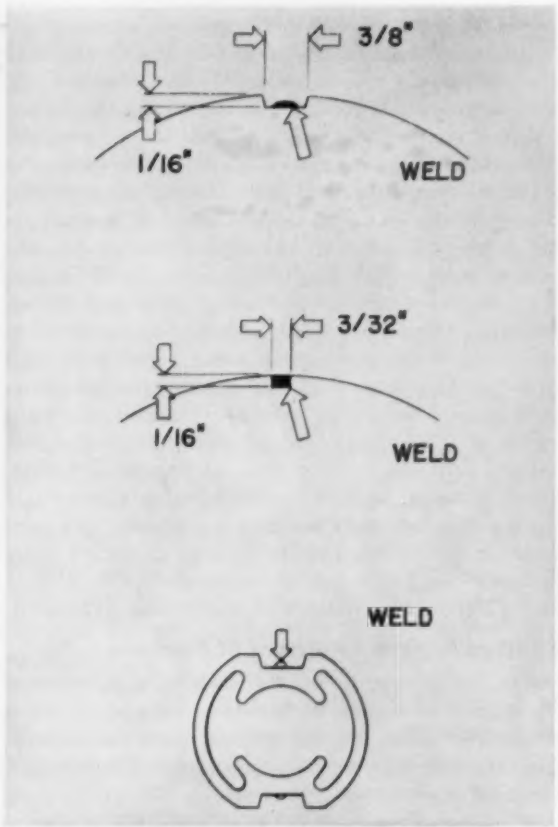


Fig. 7 Consumable-electrode inert-gas welding requires a notch or depression when the weld is made on the outside diameter of a stator

Joint Designs

The application of the inert-gas-welding processes to the manufacture of electric motor stators requires that consideration be given to the type of joint design to be used. The type of motor and the geometry of the stator to a large degree will determine the proper joint design and weld bead location. Basically, the weld beads should be located where they will offer the least interference to the magnetic path of the stator. Secondly, the weld beads should be located in such a manner that they produce a constant-height stator. Welds placed too far apart will result in bridging or springing between welds.

Figure 6 illustrates several popular joint designs currently being used for welding stators by the nonconsumable-electrode inert-arc process.

1. The first and least complicated of these joint designs consists of a weld bead on the outside diameter of the stator. The penetration of the weld is below the finished machined diameter of the stator, so that the machining operation does not remove the weld entirely.

2. The second joint design consists of placing a weld across the laminations in the base of a groove. The groove may be square or may be semicircular in contour, and should have sufficient depth below the surface so that any machining operation on the outside diameter does not remove the weld.

3. Special patented joint designs consisting of a projection within a notch have been used by some manufacturers. The projection places the weld outside the magnetic path of the stator and provides an ideal joint condition for high-speed welding.

4. Some manufacturers have taken advantage of the U-grooves used for clearance around the assembly bolts on the motors. The U-groove is ideally suited for the inert-gas metal-arc welding process since no metal buildup occurs during the welding operation, and clearance is maintained in the groove.

5. The electric motor industry manufactures many motors that are not circular in their outside shape. These motors provide natural locations for weld beads on flat surfaces, corners or depressions in their outer diameter.

The consumable-electrode inert-arc welding process differs from the nonconsumable electrode process in that it requires a notch or a depression such as shown in Fig. 7 in which to deposit the filler metal when the weld occurs on the outside diameter of the stator. On stators where the outside diameter need not form a continuous surface, weld beads are placed in a groove

approximately $^{1}/_{16}$ in. deep and $^{3}/_{8}$ in. wide. On stators requiring a continuous machined surface on the outside of the stator, the consumable electrode process requires a groove $^{1}/_{16}$ in. deep and $^{3}/_{32}$ in. wide. The weld bead in this case completely fills the groove, and the outside diameter of the stator can be machined flush to form a continuous surface.

On the joint designs shown in Figs. 6 and 7, welds are made by the nonconsumable electrode process at speeds of 20 to 80 ipm. Welds are made by the consumable electrode process at speeds of 40 to 180 ipm.

Shielding Gas Selection

When welding electric motor stators with a tungsten electrode, the shielding gas is the principal consumable item required by the welding operation. Comparable weld quality can be obtained with both argon and helium shielding gases. However, helium generally requires a flow rate $2^{1}/_{2}$ to 4 times that of argon. The present economies possible with the tungsten-arc welded motor stators are a result of the very low gas flow rates with argon. Shielding gas flow rates on a production installation will range from 5 to 8 cfh with argon as compared to helium flows ranging from 18 to 35 cfh. Weld bead formation on electrical grade steel does not seem to be influenced appreciably by the gas characteristics. Welding grade argon or argon containing small additions of oxygen can be used for welding stators with the consumable-electrode process.

Welding Power Supplies

Direct current-straight polarity should be used for welding electric motor stators with the nonconsumable-electrode welding process, because it permits more efficient use of the arc power and shielding gas than a-c or d-c reverse polarity. On a multiple torch installation, the welding power supplies should permit continuously variable current adjustments, so that welding currents may be balanced for each individual torch. Arc blow problems develop if balanced currents are not used.

Fixture Requirements

We have discussed, so far, the selection of the welding process, the effect of material composition, the effect of the surface condition of the lamination, the joint design, shielding gas and welding power supplies for welding stators. All of the above would be of little value without a satisfactory mechanism for holding the laminations in proper alignment and for moving the laminations under the welding arc. A good welding fixture is an important requirement for economical application of inert-gas welding to stators.

The fixture is composed of four basic elements. These are:

1. The mandrel.
2. The lamination clamping mechanism.
3. The travel mechanisms.
4. The control circuits.

The purpose of the welding fixture is to maintain the proper space relationship between the workpiece and welding equipment. Motion is then produced by the fixture to move the workpiece, or the welding equipment, over the path of the weld while maintaining the proper space relation between the two. Sequence controls are usually employed on a mechanized welding fixture to control the flow of shielding gas, start the arc, start and stop the travel mechanism, index the workpiece, stop the arc and shut off the shielding gas. The fixture may also contain means for clamping the laminations and expanding the mandrel holding the laminations.

Figure 8 is a horizontal multiple torch type of stator pack welding machine in which the stator indexes and the torches travel with respect to the workpiece.

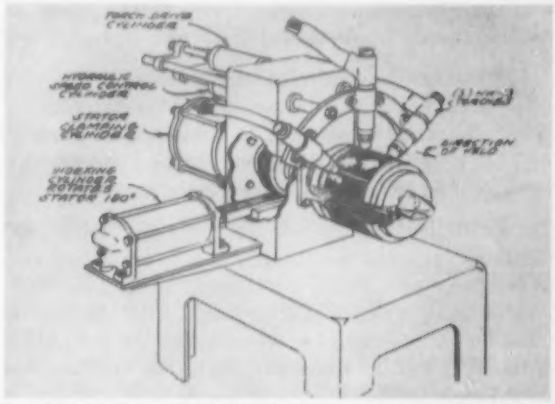


Fig. 8 A horizontal multiple torch type stator pack welding machine

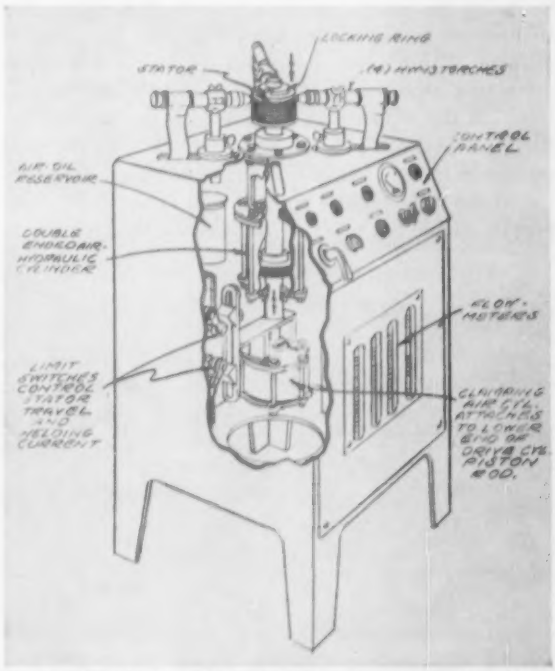


Fig. 9 A typical vertical multiple torch stator pack welding machine

Figure 9 is a typical vertical multiple-torch stator-pack welding machine. This fixture uses an air-hydraulic system for moving the stator past four stationary torches. The laminations are clamped by an air cylinder at the base of the hydraulic cylinder. Limit switches control the start and stop of the arc and operate the sequence controls of the machine. A needle valve controls the speed of the hydraulic cylinder and a four-way solenoid valve controls the direction. The fixture may be pushbutton operated and can produce 150 to 200 parts per hour with manual loading or 500 parts per hour with mechanical loading.

Mandrels

The welding fixture usually employs some type of welding mandrel to hold the laminations in proper alignment during welding. Two general types of welding mandrels are used at the present time:

1. The first is mandrel in which a solid internal plug is used to line up the bore of the stator.
2. The second consists of an expanding device similar to an Erickson machine tool collet and is used to line up the bore of the stator.

The expanding mandrel permits more rapid loading and unloading, but has the disadvantage of additional cost as compared to the solid mandrel. The solid mandrels may be easily stripped of their stator if the stator is removed within three to five seconds after the completion of the weld. If the stator remains on the solid mandrel for a longer period, the stator begins to shrink and in some cases may require a press operation to remove it.

Inert-gas metal-arc welding with nonconsumable electrode generally requires copper end plates or copper inserts in the mandrel such as shown in Fig. 10 in order to prevent fusing of the laminations to the mandrel proper. The copper end plates or inserts will determine, to a large degree, the condition of the end lamination. If the copper is cut square with the end lamination, the copper will absorb sufficient heat from the weld puddle on the end lamination to prevent good fusion between the weld puddle and the end lamination. The copper should be relieved to prevent this condition.

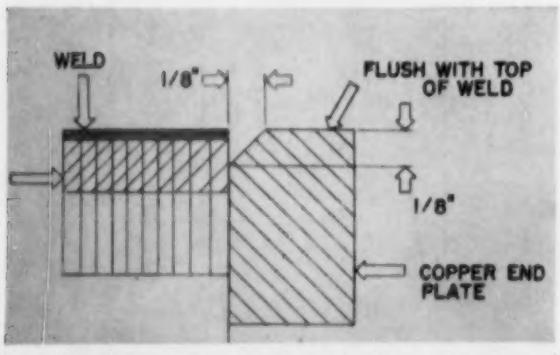


Fig. 10 Copper end plates or copper inserts are used with nonconsumable-electrode inert-arc welding to prevent fusing of the laminations to the mandrel

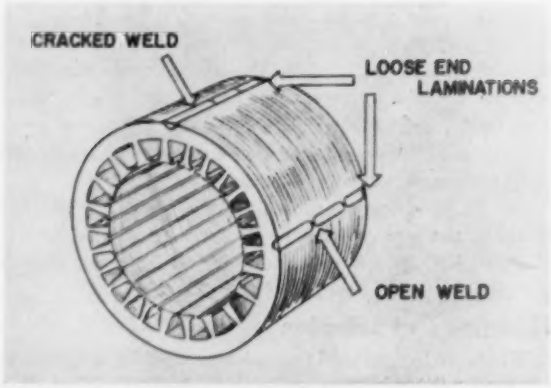


Fig. 11 Typical weld defects which may arise from following incorrect welding procedures

If the copper is appreciably below the top surface of the weld bead, the arc will have a tendency to hang onto the end lamination, and then will rapidly snap over to the copper at some point approximately 1/4 to 1/8 in. from the end. This will result in a sudden suckback of the fluid metal in the weld puddle, which may result in the end lamination not being welded to the balance of the stator.

Compression Pressure

When the laminations have been placed on the mandrel, they must be compressed to form a stator of the proper height. The magnitude of the clamping pressure is generally determined experimentally on the particular type stator that is being welded.

The clamping pressure is influenced by the shear fin produced by the stamping operation, the shape of the stator, the service to which the stator is to be applied, the type of motor, the contour and the location of the weld, the surface condition of the lamination, and the chemical composition of the steel. Clamping pressures have ranged from 250 to 6000 lb total loading of the laminations during the welding operation.

Excessive compression pressure during the welding operation tends to produce cracked welds and reduces the efficiency of the motor due to the absence of space between the laminations.

Defects

As in any fabricating operation, if the correct procedures are not followed, for each job, weld bead defects such as shown in Fig. 11 will occur. For example, excessively high shear fins, strains during the annealing operation, excessive clamping pressures or the composition of the steel, may cause a transverse weld bead crack. This crack permits separation of the lamination and in effect, fails to hold laminations rigidly together.

Magnetic arc blow, a poor ground connection, or mechanical separation of the lamination may cause an open weld. An open weld differs from a cracked weld in that the weld bead never forms continuously between the adjacent laminations.

Improper fixturing conditions may result in a weld

which does not securely join the end lamination to the balance of the stator.

The design of the fixtures will have a decided influence on the cost of manufacturing and the extent to which the welding process is applied to motor stators. Two fixtures were shown, both of which illustrate a practical approach to the problem for medium or low production and low fixture costs. However, it is necessary to think in terms of continuous type machines for maximum cost reduction and high production rates. An example of continuous machine is shown in Fig. 12.

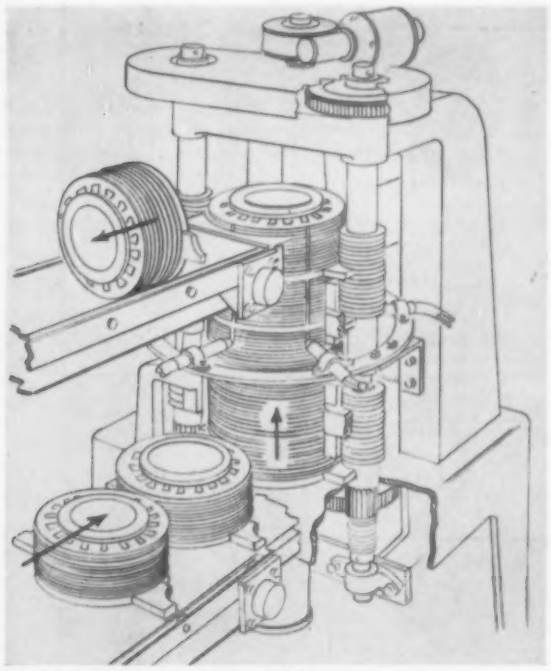


Fig. 12 A continuous stator welding machine

With this type machine, the shielding gas will be used practically 100% of the time for productive welding operation. The stators are fed into the bottom of the dual screw columns, compressed between the upper and lower screws, welded while under compression and ejected at the top of the machine.

The use of continuous stator pack welding machines will lead eventually to completely integrated press and welding lines for stamping and welding the lamination together to form the finished stator. Continuous operation will permit a further reduction in the cost of manufacturing electric motor stators and will encourage the more widespread use of the welding process.

Welding Costs

The welding costs resulting from the use of the nonconsumable-electrode inert-arc process to weld electric motor stators are primarily associated with the cost of the shielding gas and the flow rates used during the welding cycle. The power consumption is usually estimated at $^1/_2$% of the shielding gas cost and the tungsten electrode consumption is estimated at $1^1/_2$% the total cost of the shielding gas. The three nomograms, A, B and C, shown on page 566 may be used to rapidly estimate the cost of the shielding gas for any combination of welding speed, stator height, number of welds per stator, shielding gas flow rate and shielding gas cost per cubic foot. Certain basic information is required in order to use the nomogram:

Information	*Nonogram*	*Column*
1. Welding speed, ipm	"A"	A
2. Stator height, in.	"A"	B
3. Number of welds per stator	"B"	D
4. Argon flow rate, cfh	"C"	G
5. Total nonwelding gas flow period, sec.*	"C"	F

* To find the gas flow period required to cool the tungsten electrodes, use the following formula for multiple torch installation:

$$NE \times 5 = \text{Total cooling time, sec}$$

where NE = number of electrodes and 5 = 5-sec electrode cooling time

Use of Nomograms

1. *Welding speed*—The estimated or actual welding speed should be located on nomogram "A," Column A.

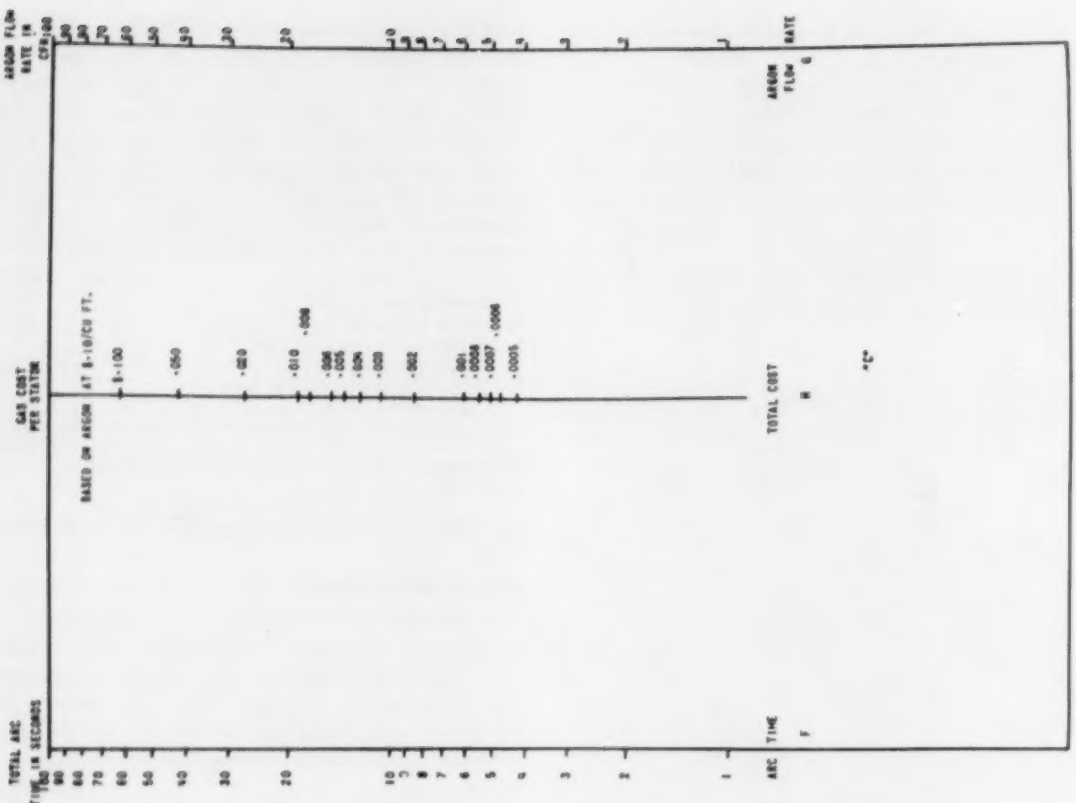
2. *The stator height* should be located on nomogram "A," Column B. A straight line drawn between these two points, continuing until it intersects Column C will indicate the arc time in seconds to make one weld on the stator.

3. *The number of welds per stator* should be located in Column D of nomogram "B." A straight line connecting the arc time in Column C with the number of welds per stator in Column D will develop the total arc time per stator in seconds in Column E. The total arc time in seconds found in Column E is then transferred to Column F of nomogram "C."

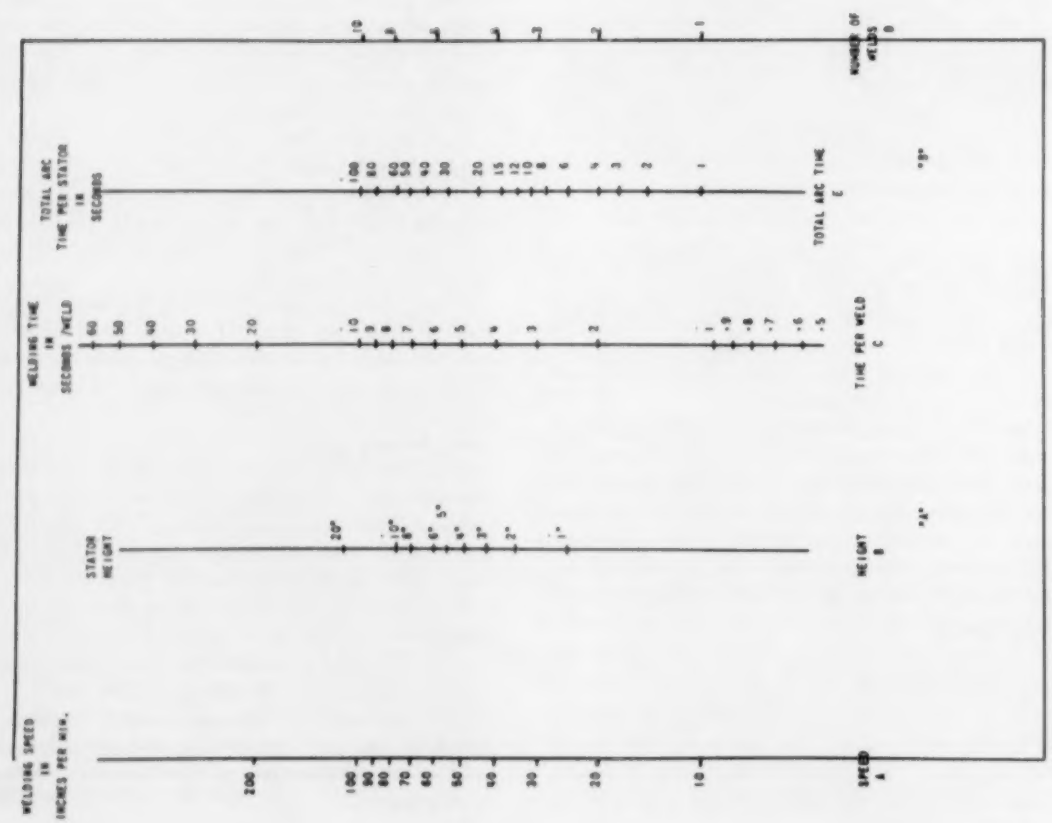
4. *The argon flow rate* required to produce a satisfactory weld is located in Column G of nomogram "C." A straight line connecting the arc time in Column F with the argon flow rate in Column G will indicate the total cost of shielding gas used during the welding period in Column H of nomogram "C."

NOTE: The shielding gas cost is established on the basis of $0.10 per cu ft. For each $0.01 difference in cost per cubic feet of shielding gas above or below the $0.10 figure used in the nomogram, add or subtract 10% of the indicated value.

5. *Electrode cooling time*—The hot tungsten electrode will require shielding gas to flow after the arc is extinguished. Generally, 5 sec is required to cool electrodes for stator pack welding. Using the formula $NE \times 5$ = total cooling time, in which NE represents the number of electrodes and 5 represents the cooling time in seconds, the total time for electrode cooling is then located in Column F, nomogram "C." A straight line is then drawn between the electrode cooling time in Column F and the shielding gas flow rate in Column G (use same gas flow rate as used for welding). The shielding gas cost for cooling the electrode is then read at the intersections of the line with Column H, nomogram "C."



Nomogram C



Nomograms A and B

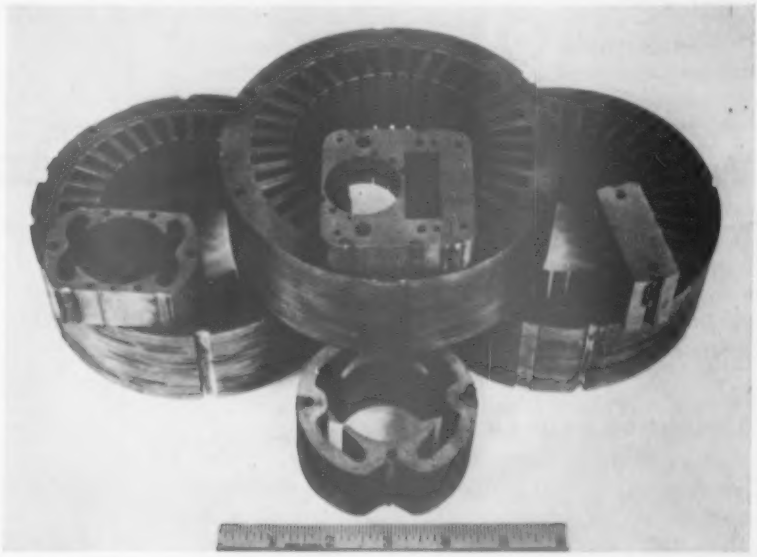


Fig. 13 Typical examples of stators being welded by the nonconsumable-electrode inert-gas process at the present time

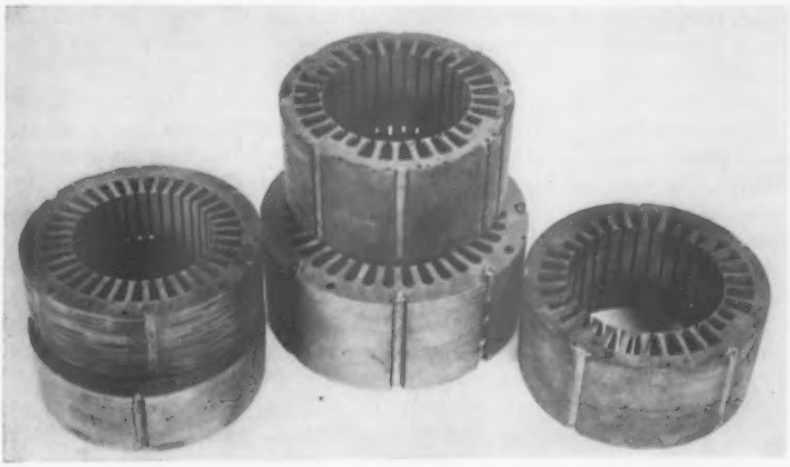


Fig. 14 Experimental stators welded by the consumable-electrode inert-gas process, which have been introduced to date

The cooling gas cost is then *added to the shielding gas cost* consumed during the welding operation for the total cost of shielding gas used to complete the weld.

Degreasing

It is general practice in the electric motor industry to use various forms of die lubricants when stamping the laminations for stators. The presence of die lubricants on the surface of the laminations to be welded may have an appreciable effect on the economics of inert-gas welding electric-motor stators. The carbonaceous material should be removed by suitable degreasing methods prior to welding. Production information indicates that the failure to degrease properly before welding may increase the shielding gas cost by 100% and the tungsten electrode consumption by 300%. The more rapid consumption of tungsten will also result in a greater number of defective welds since the more frequent electrode adjustments will be required and the operation will necessitate closer surveillance by the welding operator or supervisory personnel.

Illustrations and Conclusions

Figure 13 illustrates typical nonconsumable-electrode arc-welded stators that are being welded at the present time. They represent hermetic motors, horsepower, fractional and subfractional motors, specialty motors for saws, sanders, drills, vacuum cleaners, electric shavers, fans, toy trains, transformers, watt-hour meters, clocks, aircraft and automotive motors and generators. In the future, such items as relays, contactors, solenoids, electric brakes, chokes, reactors and transformers may be fabricated by this process.

Figure 14 illustrates experimental consumable-electrode arc-welded stators that have been introduced to date. These welds are made in the vertical position only at speeds of 90 to 120 ipm.

The results obtained so far indicate quite conclusively that both inert-gas metal-arc welding processes warrant a close examination by electrical manufacturers as a means for fabricating laminated assemblies used in a great variety of products.

WELDING TYPE 347 STAINLESS STEEL FOR 1100° F TURBINE OPERATION

A postulated mechanism relating "hot cracking," during welding, with the composition of 18–8 stainless steel

DISCUSSION BY JOHN M. PARKS

The authors are to be congratulated for conducting an excellent investigation and presenting a very interesting paper. As they have discussed, residual free ferrite in 18–8 stainless steel weld metal is used as a means of preventing "hot cracking" of weld deposits. If weld metal composition is adjusted, following the Schaeffler diagram,[1] to produce 10% residual ferrite, complete ferritic envelopes about the austenitic grains may form; if the steel is heat treated or subjected to temperature service, this ferrite transforms to sigma phase and the resulting metal exhibits brittleness. By reducing the amount of free ferrite residual to approximately two and one-half per cent by adjusting the electrode composition, the ferritic envelopes are less continuous and an improvement in impact strength and ductility is effected, as shown by Fig. 5 in the authors' paper.

During recent years, a number of studies of stainless steel weld metal have been made. The work of Thomas and Campbell,[2] and this paper appear to be the best ones, in the writer's opinion; they clearly report experimental observations and summarize and analyze observed trends. Unfortunately, little has been said about the mechanisms which function to produce the observed results, and why such elements as silicon and phosphorus, and carbon operate to increase or reduce "hot cracking" tendencies of stainless weld metal. As time passes and more empirical data are collected, there is an increasing need to establish the theory underlying these phenomena, so that:

1. The pattern of relationships may be more readily grasped by the welding engineer.
2. Future research on the subject may be more efficiently evolved by the research scientist.

For some time, the writer has been attempting to analyze the observed relationships related to the problem, and has found the low melting silicate film theory inadequate as a means of predicting composition sensitive effects. As a result, the writer has gradually evolved a basic postulate which agrees with all of the observations of which he is aware. In view of the absence of discussion on this aspect of the problem, this hypothesis is offered to explain the results of the authors and the empirical relationships developed by Messrs. Thomas and Campbell, with the hope that such an hypothesis will be of value and may provoke further comment and study.

Molten 18–8 stainless steel weld metal will contain approximately 18 to 22% Cr, 8 to 10% Ni, 0.06 to 0.20% C, 0.025% P, 1.5 to 4.0% Mn and 0.25 to 0.80% Si, as well as additions of columbium, molybdenum, titanium and other alloy additions depending upon the grade of steel. In addition, nitrogen may be intentionally added and traces of oxygen, hydrogen, reduced flux components and other unintentional elements may be present. When the metal solidifies and cools, each of these elements undergoes the microsegregational influences that are dictated by the phase changes and the free energies of the solution in each of the phases in question. Unfortunately, an exact understanding of the phase relationships existing for many of the commercial grades of stainless steel are not known, and few students of stainless steel have ever given consideration to establishing the free energies of solution for the temperature ranges wherein "hot cracking" is observed.

Krivobok,[3] Fig. 1, gives the expected phase relationships for the 18Cr–8Ni alloy as a function of carbon content. These will suffice for the purpose of this discussion. With regard to free energies of solution and distribution ratios of elements like silicon, phosphorus, carbon, chromium, and other important alloy ingredients in melt, delta ferrite and austenite, several general observations may be made. Studies of binary phase diagrams of iron classify silicon, phosphorus, chromium, molybdenum, tungsten, vanadium, columbium, tantalum, titanium and zirconium as ferrite

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Paper by R. M. Curran and A. W. Rankin was published in the March 1955 issue of THE WELDING JOURNAL, pp. 205–213.

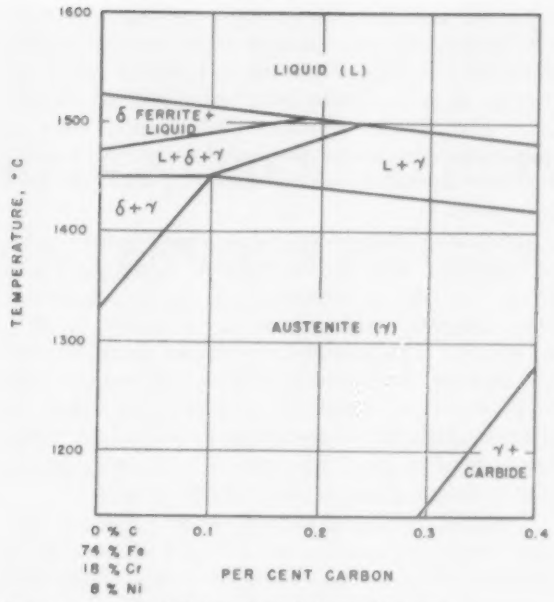

Fig. 1 The 18–8 stainless steel phase diagram

formers, and carbon, nickel, manganese and nitrogen as austenite formers. In other words, such a classification indicates that ferrite formers have a solution preference for ferrite, and austenite formers a solution preference for austenite. Presented another way, in a duplex ferrite-austenite steel structure, ferrite formers would tend to concentrate in the ferrite and austenite formers in the austenite.

When molten 18–8 weld metal of low carbon content (say 0.10% C) starts to solidify, delta ferrite is the first solid phase to form. It grows to a volumetric size described by the phase diagram, Fig. 1, the densities of the phases involved, and the carbon content of the alloy. At the peritectic temperature, the residual melt decomposes into austenite and ferrite, or reacts with ferrite to form austenite, depending upon the carbon content.

During these transformations, the alloy components are selectively absorbed in each phase that forms; for example, appreciable quantities of silicon, phosphorus and chromium collect in the ferrite phase as it forms. As the weld metal continues to cool, the delta ferrite phase is progressively absorbed by the austenite; iron atoms along the grain boundary between the two phases readily leave the body centered lattice and join the face centered lattice. However, the ferrite formers, which occupy substitutional positions in the ferritic lattice, find this transformation difficult because they cannot readily accommodate themselves to the austenitic lattice. As a result, they tend to concentrate in the ferrite along the transforming interface between ferrite and austenite. Here they tend to delay transformation until they diffuse toward the core of the shrinking ferrite crystal, where they concentrate and stabilize the last residual ferrite.

Structurally, as the ferrite is absorbed by the austenite, the residual ferrite is located by the process at the austenitic grain boundaries. If the welding electrode is unbalanced so as to give little or no residual ferrite, the phosphorus and silicon concentrations will increase to a concentration which can liquate the residual ferrite as indicated by the iron-phosphorus and iron-silicon diagrams. This liquation at the austenitic grain boundaries, coupled with the stresses associated with welding constraint, results in "hot cracking." The basic purpose of residual ferrite in austenitic weld metal is to serve as a sponge to hold the percentage of silicon and phosphorus below a critical level in the grain boundary ferrite, and thereby avoid its liquation. This concept is illustrated in Fig. 2.

The amount of residual ferrite required to prevent "hot cracking" is determined by the phosphorous and silicon content in the molten weld metal and the maximum amount of ferrite that forms at the peritectic temperature. Lower silicon and phosphorous contents

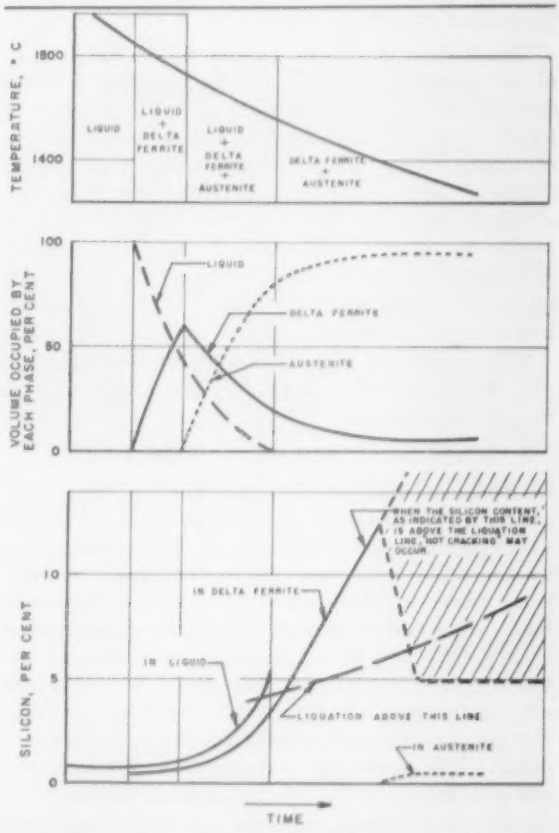

Fig. 2 The postulated mechanism of "hot cracking" in 18–8 stainless steel weld metal, considering silicon as the cause of delta ferrite liquation

The upper figure shows an assumed cooling curve for 18–8 weld metal, the middle figure the percent by volume of each phase existing as the weld metal cools and the lower figure the average percent silicon in each phase. The lower figure indicates that the percent silicon in delta ferrite rises rapidly as the weld cools and the volume of the residual ferrite diminishes. In this visualization, the distribution ratio between delta ferrite and austenite was assumed to be ten. No consideration of non-homogeneous austenite below 1400° C was made; if diffusion of silicon into austenite is slow below this temperature, the percent silicon in the ferrite will stay above the liquation composition at much lower temperatures than indicated and "hot cracking" should be assured.

in the weld metal should permit the use of electrodes formulated to produce less residual ferrite; such a relationship is generally recognized by stainless steel electrode manufacturers. The function of carbon is to control the maximum amount of delta ferrite that may form as a weld deposit solidifies, more carbon in the alloy giving a lesser amount of ferrite at the peritectic temperature, in accordance with the inverse lever law for two phase regions of phase diagrams.

Consider two stainless steel weld puddles both containing 0.75% Si, one with 0.10% carbon, the other with 0.20% C, and both balanced to give 5% residual ferrite. The one with 0.10% carbon will form a maximum of say 70% delta ferrite, whereas, the 0.20% carbon steel will form a maximum of 15% delta ferrite. At the time of maximum ferrite formation, if the silicon content of the delta ferrite is 0.70% Si, and if no silicon is lost as the volume of ferrite contracts, the final silicon in the two ferrites will be 9.8 and 2.1%, respectively. The first steel should "hot crack" in the weld metal, the second should not.

This basic concept is in agreement with the findings of the authors; namely, in a fully austenitic stainless steel electrode deposit, if a silicon-to-carbon ratio of less than five is maintained, a sound deposit will be obtained. The carbon lowers the total volume of delta ferrite that forms, and therefore lowers the total amount of silicon that is absorbed by this volume, and thus lowers the amount that would be available later for liquation of the ferrite as the ferrite crystal is absorbed by austenite on cooling. In other words, higher carbon content neutralizes the effect of higher silicon.

This hypothesis is also in agreement with the concept of Thomas and Campbell[2] which was incorporated in their expression:

$$\%P + 0.04\ (\%\ Si - 0.25) = 0.030\%\ (max.)$$

for the limits on the maximum silicon and phosphorus to assure an all austenitic arc welding electrode deposit which does not show a loss of tensile strength in an "all weld metal" test specimen, the drop in tensile strength being related to "fine fissures" as a result of "hot cracking." This work evaluated the effects of composition on 25% Cr – 20% Ni weld metal having a nominal content of 0.14% C, 1.50% Mn and 0.007% S. In such an alloy, only a small amount of delta ferrite forms on solidification, but this amount is adequate to establish a condition for "hot cracking" if the silicon and phosphorous contents are above the low limits given by the aforementioned expression. Raising the carbon content of this metal to 0.30% C will permit these limits on silicon and phosphorus to be raised.

An analysis of the influence of the other ferrite forming alloy components in 18–8 stainless steel reveals that they should not be expected to cause the residual ferrite to liquate by concentrating in the ferrite; their binary phase diagrams do not show low-melting eutectics as do phosphorus and silicon. However, it would be expected that they would concentrate in the ferrite, and thus become selectively distributed in the region of the austenitic grain boundaries when the ferrite is completely absorbed, and would, therefore, be more readily available to form carbides when later the carbon is rejected to the austenite grain boundaries by the carbide precipitation process. Undoubtedly, such a mechanism contributes to the effectiveness of small amounts of columbium in stabilizing stainless steel weld metal.

It has long been recognized that the residual ferrite in austenitic weld deposits readily forms the sigma phase. To do so, according to the iron-chromium phase diagram, its chromium content should be 30% or greater. The proposed mechanism for silicon and phosphorous concentration in delta ferrite should also be applicable to chromium; it provides a means for achieving high-chromium-bearing ferrite in weld metal; ferrite which on heat treatment, should readily decompose into sigma phase as was found by the authors.

Austenite formers, like carbon, should alter the complex phase relationships by lowering the maximum amount of delta ferrite that forms during solidification and cooling of the weld deposit. They do this in two ways; namely, by lowering the melting point of the alloy to temperatures below which delta ferrite is stable, and secondly, by selectively stabilizing the austenitic phase.

Considerable research and evaluation, undoubtedly, will be required to substantiate the aforementioned hypothesis and establish more exactly the quantitative effects of each of the elements in the complex metallurgy of austenitic stainless steel. Fortunately, such tools are now becoming available; for example, the Rensselaer "hot tensile" test, discussed by Nippes, Savage, Mason and Bastian,[4] and its application to stainless steel, along with electron diffraction studies of such fractures, could be of great significance. In time we shall know; until then, it is the writer's hope that the above concept will be of value to those trying to make sense out of the array of data in the literature.

AUTHORS REPLY

Dr. Parks has presented a refreshingly new hypothesis to explain the reasons for cracking in fully austenitic 18–8 weld deposits and the effectiveness of ferrite in the final microstructure in preventing such cracking.

The authors would like to express their appreciation to Dr. Parks for his very excellent discussion and join him in hoping that his postulated mechanism will stimulate investigations which will lead to a better understanding of the behavior of fully austenitic weld metal compositions.

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HIGH-NICKEL OVERLAYS ON FERROUS METALS

DISCUSSION BY SIDNEY LOW

The authors are to be complimented on their effort to compile the data on high-nickel overlays on ferrous metals in one article. However, we cannot agree with their conclusions relative to high-nickel overlays on cast iron.

Many commercial applications of Monel overlays on cast iron indicate that high quality, defect free deposits can be made on both unalloyed and alloyed cast iron if a barrier bead of "Ni-Rod 55" or "Ni-Rod" is employed. There are some data to indicate that "Ni-Rod 55" offers certain advantages over "Ni-Rod."

As a general rule, a stress relieving treatment of the cast iron, prior to overlaying, will minimize cracking of the base metal. This is particularly true of highly restrained sections requiring rather large overlays.

Typical compositions of weld metal and base metal are shown in Table 1.

Figure 1 illustrates a transverse section through an overlay on Class B cast iron. The overlay technique was as follows:

Sidney Low is associated with Chapman Valve Mfg. Co., Indian Orchard, Mass.

Paper by Pease, Bott and Waugh was published in THE WELDING JOURNAL, **34** (1), 40–48 (1955).

First bead—"Ni-Rod 55 in., 1/8 in. diam., D.C.R.P., 1 pass, deposit thickness—approx. 3/32 in.
Second and third bead—Monel 130, 1/8 in. diam., D.C.R.P., deposit thickness—approx. 5/16 in.

Fig. 1 Transverse section, Monel overlay on class B cast iron. Etch—1 : 1 nitric acid, 70° F. × 2

(a) Base metal. "Ni-Rod 55" interface. (b) "Ni-Rod 55." First pass Monel interface. (c) First pass Monel; second pass Monel interface.

Fig. 2 Interfaces. Unetched. × 100

Table 1

Material	*Total, C*	*Mn*	*Si*	*P*	*S*	*Ni*	*Fe*	*Cu*
ASTM A126-42, Class B	3.25	0.80	2.00	0.15	0.10	...	Bal.	...
ASTM A278-51T, Class No. 50	3.20	0.70	1.40	0.08	0.07	1.75	Bal.	Cr-0.50 Mo-0.70 Cu-0.0
"Ni-Rod 55" overlay on Class B cast iron	2.07	0.62	0.71	0.069	0.025	41.58	Bal.	...
"Ni-Rod" overlay on Class B cast iron	2.09	0.50	1.10	0.062	0.030	59.7	Bal.	...
Monel No. 130 overlay on "Ni-Rod 55" weld metal	0.25	1.80	0.40	0.021	0.017	64.3	2.40	Bal.

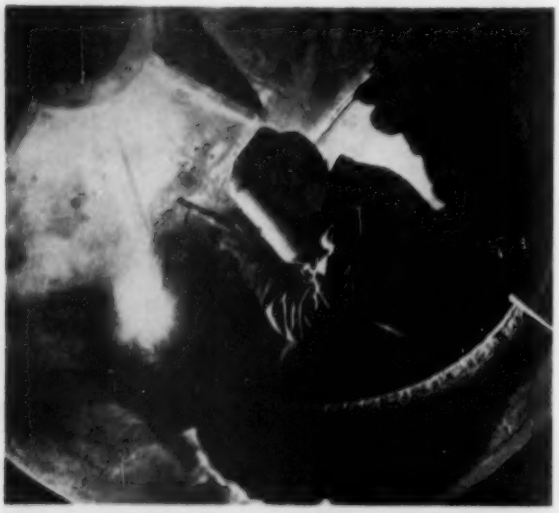

Fig. 3 Class 50, 42-in. cast-iron automatic cone valve. Monel overlay being deposited on seat

Figure 2 illustrates the several interfaces.

Shear and tensile test results indicate that the weld metal—base metal bond strength is equal to the strength of the weaker material (the cast iron).

Figure 3 illustrates a 42 in. Class No. 50 cast iron valve with both the seating surfaces on the plug and body consisting of Monel weld metal overlayed on the cast iron using the technique illustrated in Fig. 2.

AUTHORS' REPLY

The authors are grateful to Mr. Low for his comments. Apparently, an important existent commercial application for Monel electrodes has been overlooked. When attempts were made to apply crack-free overlays on cast iron, the authors' test results were consistently discouraging. However, the procedure which Mr. Low and his associates have used with good results was not investigated by us. They are to be commended for their success in steering a careful path through the hazards and reach a useful end point.

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HARD-FACING PROCESS PERMITS RECLAIMING OF PIPE LINE FLOW-CONTROL VALVES

Service life of plug-type line valves is being prolonged by hard facing at Hamer Valves, Inc., 2919 Gardenia Ave., Long Beach 6, Calif. These valves control fluid flow in all types of pipe lines.

According to the Wall Colmonoy Corp., a process which is a combination of spraying and welding is being used to reclaim the valve plugs with three reported advantages: (1) warpage is not a problem so there is no need for overlaying the plug neck packing area as required in the conventional procedure; (2) rough machining before finish grinding is no longer necessary; and (3) the extra steps required for application of hard-chrome plating are avoided.

Prior to using this special process, an arc-welded overlay method was used by Hamer for rebuilding worn and damaged valve plugs and valve bodies. The valve body seating surface was arc welded, rough machined and ground oversize to clean up (0.125 in. on diam permissible). After removal of the original hard-chrome tapered surface, the valve plug was rebuilt with an arc-welded overlay to produce an oversize dimension.

Because of possible warpage of the welded plug, the packing area on the plug neck also had to be built up and then machined to run concentric with the plug taper. The plug was next rough machined and the tapered seating surface ground 0.006 in. smaller on the diameter than the oversize rebuilt valve body. The plug then had to be chrome plated and finish ground to precisely fit the seating surface of the valve body.

In the new process, the valve body seating surface is still arc welded and ground oversize. In reclaiming the plug, the original chrome-plated tapered seating surface is first removed. Where necessary, cavities and surface imperfections are arc welded and rough machined. The surface to be hard faced is then grit blasted, acid dipped, soda dipped, steam cleaned and airblast dried to provide a surface that will insure positive mechanical bonding of the sprayed deposit to prevent its lifting during fusing.

Plug valve components reclaimed by hard facing. *(Left)* Plugs with fusion bonded alloy coating before grinding; *(right)* smaller coated plugs after finish-grinding operation

The plug is mounted between centers on a small lathe and rotated; the hard-facing alloy powder is applied with a pistol-type spraying unit. Overlay thickness depends upon the oversize conditions of the rebuilt valve body.

While still rotating between centers on the lathe, the plug is heated with the flame of an oxy-acetylene torch to a surface temperature of approximately 1900° F, just enough to fuse the overlay but not enough to cause warpage. The result is a fusion-bonded nonporous welded overlay that is high in abrasion, corrosion and impact resistance. The coated plug is then finish ground to match the valve body.

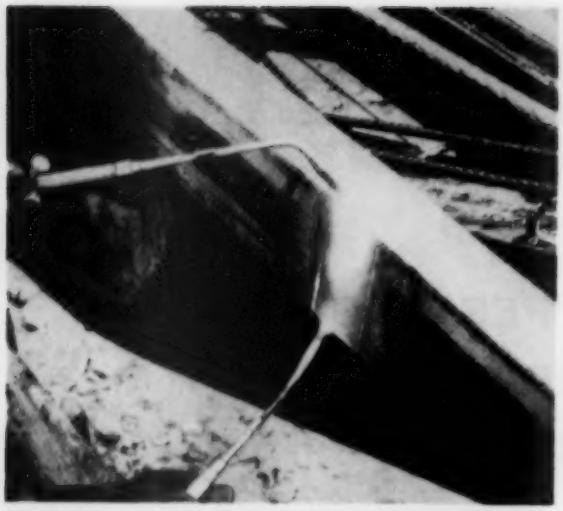

Fig. 1 A bow in the dipper sticks of the large shovels results in loose motion and inaccurate control of the bucket. Here, two operators are heating a section of a dipper stick, as part of the straightening operation

Fig. 2 Toward the end of the straightening operation, a wire is stretched down the dipper stick, and a sight test for straightness is made. The average time required to straighten two dipper sticks is 3 hr

OXY-ACETYLENE FLAME KEEPS HEAVY EQUIPMENT IN TOP SHAPE

BY J. DEAN DAVIDSON

Mining and moving thousands of tons of rock is hard on even the heaviest of equipment. The shovels, graders and crushers used for quarrying, as well as for road construction and large building jobs must be inspected periodically, and repaired whenever they are not in top working condition. A large percentage of the maintenance performed on heavy machinery is done with oxy-acetylene equipment. Oxy-acetylene heating, welding and cutting have a great part in lowering the repair and operating costs of heavy equipment.

Many of the maintenance jobs which require heating are straightening operations. Over a period of time, heavy loads of rock tend to bend the frames and operating shafts of some of the equipment. The flame-straightening procedure for these steel parts is the same as the procedure used for any structural members.

The first step is to mark off a vee-shaped area at the point of the bend. The wide area of the vee should be at the outside of the bend. The size of the vee depends upon the amount of straightening needed.

Then apply heat, either with a large-sized welding torch or with a special heating head. Allow the part to cool in the air, and notice how the member bends due to contraction of the heated area. The ends of the part will be pulled toward the wide side of the vee.

Check the alignment of the part. If it is still out of line, use the same procedure, and heat an area next to the first vee. To get maximum straightening it is sometimes desirable to cool the heated area quickly. This can be done with either a water or compressed air quench. Ordinary low-carbon steels may be heated to about 1600° F (bright red) and quenched in this way without harm.

Flame-straightening operations are standard maintenance procedures in quarries, where minimum loss of time and maximum equipment efficiencies are the goals. The total time required to flame-straighten both dipper sticks of a large shovel is approximately 3 hr, for an oxy-acetylene operator and helper. This time can be fitted in when the shovel is idle, since no dismantling is required.

Braze Welding Plays Its Part

Quarry work is jarring. Heavy rock that is constantly loaded and unloaded creates extra strain for the cast parts of machinery. When equipment checks show cracks in cast-iron castings, immediate repair is performed.

Before any cast-iron casting is braze welded, it is

J. Dean Davidson was formerly associated with Stone Mountain Grit Co., Lithonia, Ga.

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Fig. 3 This pittman shaft of a large jaw crusher is approximately 10 ft long and weighs $1^1/_4$ tons. Under heavy crushing conditions the shaft becomes bent. Here, two operators are beginning the straightening operation

Fig. 4 The caps that fit the large pittman shafts are water cooled. Here, the operator is veeing a crack on the outside of a pittman cap by flame gouging. After veeing, the crack will be cleaned and braze welded

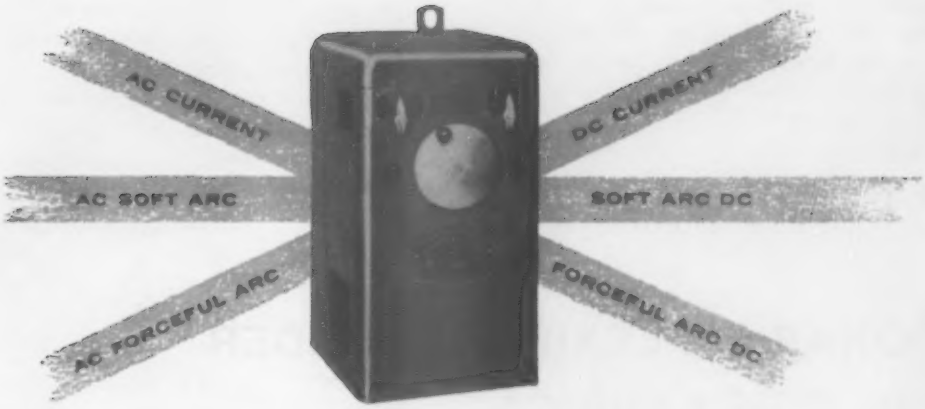
carefully prepared. The area to be welded is cleaned thoroughly, and the crack is veed. Veeing the crack can be done either by grinding or chipping.

For braze welding, adjust the flame so it is slightly oxidizing. Heat the end of the crack until the metal just turns a dull red. When the base metal is at the right temperature, tin the weld area by melting the rod on to the surface of the crack. The success of braze welding depends first upon complete tinning of the base metal with the bronze rod, and then upon proper weld buildup. The bronze weld metal should be completely fused, and the proper flux should be used in order to assure a clean weld.

The use of braze welding for immediate repairs is

Fig. 5 In this picture, the operator is braze-welding a crack on the inside of a pittman cap. By using braze-welding for repair, the quarry keeps spare parts stock to a minimum, and still has equipment in top shape

Fig. 6 Using an oxy-acetylene cutting attachment, the operator is trimming a churn drill bit. The inner courses of these bits gradually close under repeated sharpenings. Then they must be trimmed to proper contour and depth



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The welding head of the machine, a standard unit, is positioned on a small portable fixture as shown in the accompanying illustration. Turning one knob raises or lowers the unit to desired heights, another moves the head sideways, while another moves the head back or forward. A turning jig rotates the pipe joint below the head during welding. First, tack welds are made on the joint, followed by a continuous three-pass butt weld. The welding head is set at a $7\frac{1}{2}$-deg angle to the work. Welding speed is 15–20 ipm. Grade 50 composition protects the weld from the atmosphere. The result is a clean, sound weld that requires no finishing operations.

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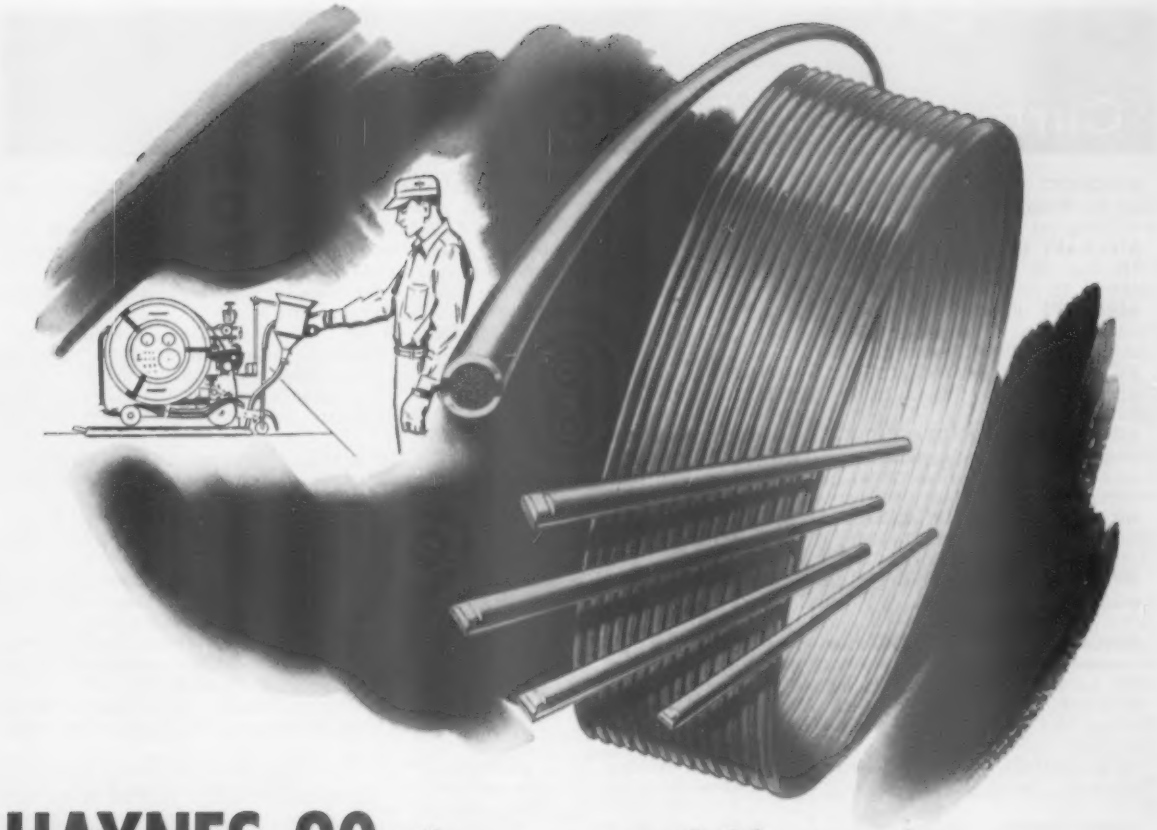

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Originators not Imitators

"Every kind of imitation speaks the person that imitates as inferior to him whom he imitates, as the copy is to the original."

LORD SOUTH

R. D. WASSERMAN

In every age, in welding and every other industry, you will always find those whose only claim to fame for their product is that it is "just as good."

More than 50 years ago, J. P. H. Wasserman, a Swiss metallurgist, pioneered a new welding process. He blazed a trail that others have tried to follow. Relentless research following his discovery has perfected Eutectic's "Low Heat Input" metal joining process which provides the high strengths of fusion welding using the lower heats associated with brazing.

Eutectic's research has never been equalled. Very simply, that is why no welding material imitating Eutectic "Low Temperature Welding Alloys"® can be "just as good" or produce equal results and savings.

Year after year, "Eutectic" has originated new and ever better welding alloys to make the weldor's job easier and more profitable. The rapidly increasing preference for Eutectic "Low Temperature Welding Alloys" in over 87,000 American industries is proof of Eutectic's leadership. It is testimony that the American weldor is too smart to be fooled by imitations. The American weldor realizes the importance of getting all the advantages of Eutectic's "Low Heat Input" metal joining process—the elimination or minimization of overheating, warping, distortion, embrittlement and other dangers to base metals.

To the weldor, Eutectic's process means "Better, Faster, Cheaper" welding. To industry everywhere, "Eutectic" has made welding more profitable, with an ever-widening field of application for production, maintenance, and salvage of metals and metal products.

A FEW "EUTECTIC" FIRSTS
1940-1955

- U.S. Patent #2,583,163. Surface alloying process for bonding metals.
- U.S. Patent #2,288,869. First Torch Alloy for joining cast iron with color match, without fusion.
- U. S. Patents #2,359,813, #2,410,850, #2,544,000. First metallic film coating to enhance Surface Alloying.
- U. S. Patents #2,279,282, #2,279,283, #2,279,284. First nickel bearing copper alloy for high tensile joining.
- U.S. Patent #2,481,053. First torch alloy and flux for joining high aluminum content metals.
- U.S. Patent #2,471,803. First "Low Amp" coating for metal arc welding of cast iron.
- U.S. Patent #2,632,000. First low-melting electrode for high tensile strength joining of aluminum.
- U.S. Patent #2,632,635. First "Low Amp" electrode coating for high alloy steels.
- U.S. Patent #2,626,339. First electrode for high tensile strength joining of copper.

RESEARCH—BACKBONE OF PROGRESS

During the past 50 years Eutectic Welding Alloys Corporation has learned much. It is possible for imitators to analyze our welding rods, electrodes and fluxes. But originating and producing them is an achievement only "Eutectic" can claim. Only the long, hard road of doing it can provide the answers.

The time spent in imitation is lost, for long before a "copy" can be made, Eutectic's research has perfected other new and improved materials.

Today, in Eutectic's own research centers throughout the world, teams of metal experts create and test newer and better products for metal joining. And, in its own up-to-the-minute plants, meeting rigid standards of quality, "Eutectic" turns these research products into "Low Temp" EutecRods and "Low Amp" EutecTrodes that bring the benefits of "Low Heat Input" metal joining into shops everywhere.

ORIGINATORS NOT IMITATORS

The research behind this phrase is your assurance of the most modern welding materials known. It is your assurance that you are applying advanced "Low Heat Input" metal joining techniques to your critical work and that you have the most modern developments at your fingertips.

Don't be fooled by those who tell you that any ordinary rod or electrode is "just as good." Nothing can match Eutectic "Low Temperature Welding Alloys" performance.

Remember, it is the quality of the finished job and the ease and speed with which you do it that determines real economy, and not a few pennies difference in original cost. Because of its research facilities, only Eutectic Welding Alloys Corporation gives you true "Low Heat Input" metal joining and helps you eliminate the risks of dangerous overheat overheating and the subsequent distortion and damage to base metals. And that, after all, is what you are always after.

READY TO PROVE WHAT WE SAY

To bring you all the benefits of Eutectic's research, our District Engineers are ready to go right into your shop to show you how Eutectic "Low Temperature Welding Alloys" will solve your welding problems. Your Eutectic District Engineer will show you how the same electrode holder or torch you use with conventional materials gives you "Better, Faster, Cheaper" welds with "Low Temp" EutecRods and "Low Amp" EutecTrodes . . . with substantial savings in work, time, gas and welding materials.

YARDSTICK OF PERFORMANCE

To help you select from among the more than 160 EutecRods and EutecTrodes those that will give you largest savings per job, write for your free copy of our new 32-page DirectoRod Guide. Whatever your welding problem—production, maintenance or salvage, there's a EutecRod or EutecTrode to do the job faster, better, and cheaper. Use the convenient coupon below.

SYMBOLS OF DEDICATION

These seals denote our proud contribution to American industry: 15 Years of "Golden" Savings, 50 Years of Research. These years have seen many great achievements. And, for the future, we look forward to ceaseless weldgineering and the continued application of Eutectic's research to your service.

R. D. Wasserman

President, Founder, Director of Research

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Society News

TAC TALKS

Resistance Welding

by J. J. MacKinney

A special Subcommittee of the AWS Resistance Welding Committee has devoted much time and effort in preparing a publication titled: "Resistance Welding—Theory and Use," which is now nearing completion.

The following remarks are taken from the Foreword of this book and clearly expound the great value it will offer those interested in the field of resistance welding:

"The application of resistance welding has rapidly expanded in the past fifteen years. While much technical data have been published to assist in making most effective use of resistance welding applications, as, for example, the AWS *Recommended Practices for Resistance Welding*, little consideration has been given to reviewing the published information of the fundamentals of resistance welding.

"Because the AWS Resistance Welding Committee believes that an understanding of the basic principles is vital to most effective use of resistance welding, it has prepared this book. While much of the information in this book has been published previously in one form or another, it is presented here in a manner that will be understandable even to someone encountering resistance welding for the first time. At the same time the book should be equally useful for more experienced designers, engineers and shop personnel.

"This book covers all the resistance welding processes. It describes the different types of resistance welding machines, electrodes and controls which are used. This book discusses the weldability of different metals and how quality control and inspection tests and methods are applied to obtain consistently good welds."

The Resistance Welding Committee under the auspices of its Subcommittee I, Welding of Low-Carbon Steel, is planning to prepare and publish a paper on "The Projection Welding of Low-Carbon Steel". The purpose of this paper is to sponsor discussion of the needs for standardization in projection welding as compared to the present disunity revealed by recent industry survey.

Full cooperation on the part of those interested in this subject is earnestly requested by the members of Subcommittee I in order to make the proposed paper profitable to both industry and AWS. It is requested that you submit any information or need for information, as the case may be, to National Headquarters. All correspondence will be given full consideration by Subcommittee I.

J. J. MacKinney is Chairman, AWS Resistance Welding Committee; Electrical Engineer, Budd Co.

Wisely Becomes Executive Secretary of ASCE

William H. Wisely took office on May 2nd as Executive Secretary of the 38,000-member American Society of Civil Engineers, oldest national organization of engineers in the country. He succeeds Col. William N. Carey, who held the post ten years and is retiring as Executive Secretary-Emeritus.

A native of Coulterville, Ill., Mr. Wisely was graduated from the University of Illinois, from which he holds the degree of B.S. and a professional degree in Civil Engineering. He has held several high ranking positions in the civil engineering field, and the first of this year he became Associate Secretary of the American Society of Civil Engineers.

GUEST FORUM

...A PLACE FOR UNCENSORED OPINIONS

An engineer is a man who can do with one dollar in one day what anyone can do with two dollars in two days. If an engineer cannot meet these specifications, his claim to the title is rather slim. There has been great lack of such engineering ability in developing the specifications and standards for arc welding. An objective review of this whole matter is necessary. The present standards are stifling progress and are needlessly costing the economy hundreds of millions of dollars per year in time and money.

This impassé has been the result of the development of standards in welding which have no application in service. For instance, we hear much about classification of electrodes, ultimate tensile strengths, elongation of deposited metal, shock tests, multidirection stresses, which have varying results and from which various testers draw diametrically opposite conclusions. There is yet to be a single standard that deals with weld behavior at the actual stresses which the welded structure will have in service.

All riveted or bolted joints are standardized for strengths that will be met with in practice. They are not at the breaking strengths of steel. Any such tests are impossible, since they are beyond the strengths that are possible to obtain.

Since riveted and bolted joints have been successful over generations with strengths which are a fraction of the strength of welds, why should we continue to use standards for Welding which require strengths far beyond these riveted or bolted strengths? Why should we continue to handicap industry by welding rules that have no possible application in actual practice? The engineer's job is to save money for the economy, not to waste it.

J. F. LINCOLN
Chairman of the Board
The Lincoln Electric Co.

Welded pipe joint being radiographed with isotope camera using iridium 192.

It's piping with problems—

so every joint is welded—and

RADIOGRAPHED

THIS UNIT of a piping system is headed for duty never faced by piping before. High pressure? Yes. High temperature? Yes.

Critical work like this calls for welding—with radiography proving every weld sound.

Welding has a staunch friend in radiography.

In high-pressure piping, in the manufacture of pressure vessels, and in structural applications, radiography continues to open up new opportunities for welding.

Radiography can help you build business, and earn a reputation for highly satisfactory work.

If you would like to know more about what it can do for you, talk to your x-ray dealer.

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Section News and Events

as reported to Catherine O'Leary

CUTTING

Phoenix, Ariz.—M. D. Stepath AWS, Arcair, Co., Lancaster, Ohio, discussed Gouging and Cutting Metal with Arc and Air at the April 20th dinner meeting of the *Arizona* Section held at the Silver Spur. Coffee speaker was Ralph Smith, director of District No. 10. Mr. Smith talked on the National Convention and the unlimited source of practical and technical information available to the members.

TOOLS AND DIES

Boston, Mass.—The annual meeting of the *Boston* Section was held on April 11th in the Westinghouse Auditorium. Announcement was made of the election of the following officers for the 1955–56 season.

Chairman—Harry J. Ferguson
1st Vice-Chairman—Herbert Turck
2nd Vice-Chairman—H. L. Toof
Secretary-Treasurer—H. S. Miller
Trustee—Harry Udin
Directors—
2 years: Charles F. Peterson
Allen J. Rosenberg
Walter A. Ovaska
1 year: Albert R. Barilaro

Following the announcement of the vote, Mr. Ferguson, chairman-elect, presented the Past-Chairman's Pin to the retiring chairman, Allen G. Hogaboom.

The coffee speaker was Saville Davis, American News Editor of the *Christian Science Monitor*, who presented an excellent talk on "Current Political Trends," bringing out a great many interesting views of the facts behind some of the latest news events.

The technical speaker of the evening was Richard K. Lee, vice-president in charge of research for the Alloy Rods Co. Mr. Lee discussed, with the aid of slides, the ever-increasing use of welding to cut the cost of manufacture and repair of tools and dies, some of which would be prohibitive if any other method were to be employed.

From the standpoint of valuable and interesting information, this was one of the best meetings of the season.

WELDED DESIGN

Cincinnati, Ohio.—The selection and crowning of "Miss Cincinnati Welder" is an annual highlight of the *Cincinnati* Section. The idea is to have a prominent entertainer from the local area made an honorary member of the Section, thereby placing the AMERICAN WELDING SOCIETY before the public eye. This year the Section chose Bonnie Lou of WLW television and radio and the star of the "Tennessee Wig-Walk" record fame. After a delightful dinner and an hour of song and festivities, the members adjourned to the auditorium of the Engineer and Scientist Building for the technical session.

Bonnie Lou, "Miss Cincinnati Welder"

District Director John Blankenbuehler had a few words of encouragement for the membership drive. Speaker "Ned" Ashton, professor of civil engineering, Iowa State University, presented numerous examples of the economy of welded designs in comparison to welded and riveted details used in the construction of bridges. The utilization of the full strength of the members by welding was stressed as an advantage over the reduced strength of members with holes punched in them for rivets. A brief history of the construction of some of the famous bridges on the North American continent added interest to the presentation.

WELDING QUALITY CONTROL

Cleveland, Ohio.—"Quality Control of Welding" was the subject of a technical talk given at the April 13th meeting of the *Cleveland* Section by R. B. Lincoln, AWS, director of National Weld Test Bureau and consulting engineer for Pittsburgh Testing Laboratory, Pittsburgh, Pa. In his talk, Mr. Lincoln covered all aspects of quality control of weld failures. Among the subjects he discussed were qualification of operators and destructive and nondestructive weld testing with emphasis on conforming to AWS, ASME and other Codes.

Coffee speaker was Ernest E. Thum, editor of *Metal Progress*. Mr. Thum explained in his talk, "Failure of the British Comets," how tests were conducted to determine the cause of failures in the pressurized planes and resulting mid-air explosions. These tests are said to be the most complete ever made in any fatality-causing failure.

METALLIC RECTIFIERS

Denver, Colo.—G. W. Willecke AWS, physicist with the Miller Electric Manufacturing Co., Appleton, Wis., was the principal speaker at the April 12th dinner meeting of the *Colorado* Section held at the Oxford Hotel. Mr. Willecke presented a lucid, easy-to-understand, description of metallic rectifiers for arc-welding machines or more specifically, selenium plate rectifiers for producing d-c current from a-c supply. The talk was very instructive and held the attention of the group for nearly two hours. It was concluded by the answering of many questions from the audience.

Coffee speaker was National Secretary J. G. Magrath who gave an outline of the progress the SOCIETY is making and the importance and recognition it has gained among other engineering organizations. Mr. Magrath gave a very inspiring talk. Mr. Magrath's story about the 10-minute limit for story tellers was very entertaining and enjoyed by all.

RESISTANCE WELDING

Columbus, Ohio.—A semitechnical

for faster,
cleaner, sounder welds use
CRUCIBLE STAINLESS ELECTRODES...

You're *sure* of fast deposition, uniform quality welds, with Crucible stainless steel electrodes. For Crucible pioneered in the *development* of stainless electrodes . . . and its welding engineers have a wealth of manufacturing and application experience for you to draw upon.

And you'll find a complete assortment of Crucible stainless bare welding rods and coated electrodes for both AC and DC welding . . . including special high carbon corrosion- and heat-resisting electrodes . . . armorize electrodes for armor plate welding . . . and electrodes for cast iron welding.

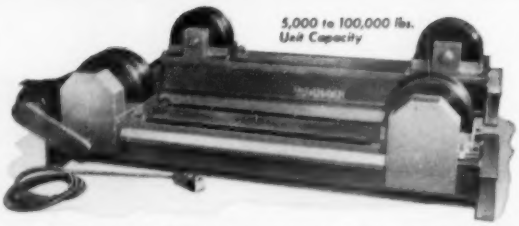
Whatever your application . . . whatever welding procedure you use . . . there is a Crucible stainless welding wire or electrode for the job. You'll get them *fast* from your nearest Crucible warehouse or welding equipment distributor. *Crucible Steel Company of America, Henry W. Oliver Building, Pittsburgh 30, Pa.*

CRUCIBLE first name in special purpose steels

Crucible Steel Company of America

Buy Aronson TURNING ROLLS
for AUTOMATIC WELDING... for MANUAL WELDING

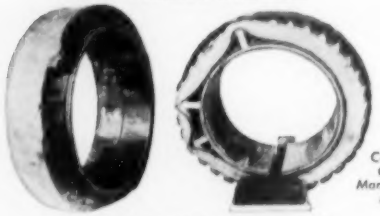
Micrometer controlled speed, ZERO to Maximum Inches Per Minute . . . Precision Cut Gears with Absolute Minimum Backlash . . . Built-In MAGNETIC BRAKING for smooth, precise stops . . . Low Voltage Control Circuit . . . Magnetic Reversing Starter and Safety Disconnect switch . . . Anti-Friction Bearings Throughout . . . Precision Steel Ways and exact diameter settings . . . Gearboxes Packed for Life . . . All Rolls stressed for 100% Overload Protection.



Any Tank or Cylinder will be somewhat out-of-round and will not be straight, but will have a belly or bend in its average centerline axis. In the case of non-rigid cylinders, multiple rolls are used to hold a true centerline, ironing out the belly or bend. When working rigid tanks and cylinders, it is best to have one roll at each end so as to avoid the belly placing all the load on the middle roll when that belly is down, and all the load on the two end rolls when the belly is up. It is to allow for the average amount of out-of-round and belly in cylindrical workpieces that ARONSON has stressed all units for 100% overload Protection. Momentary overloads as high as 100% will not breakdown an Aronson Roll.

Overloading or accidentally dropping the load on an Aronson Turning Roll WILL NOT crush and ruin the tire. The Overload Disc of steel placed on each side of each tire is smaller than the tire by just that amount which allows a safe deformation (flattening) of the tire which occurs at maximum load. Compression and tension Tie-Rods hold the Overload Discs firmly against the steel rim of the rubber tire, forming an effective axle as large in diameter as the O.D. of the steel tire rim. Crushing the tire is impossible regardless of the load put on an Aronson Turning Roll. Large Timken Bearings maintain perfect alignment.

AVOID DAMAGE LIKE THIS



Courtesy of the Rubber Manufacturers Assoc., Inc.

UNDERCUTTING CAUSED BY OVERLOADS

Continuous overloads on this tire caused the rubber to bulge out over the steel band. This, in turn, set up a shearing or cutting action in the tread. Tire is ripped around the band on both sides and is about ready to fall off.

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Aronson MACHINE COMPANY
ARCADE, NEW YORK

discussion on Resistance Welding Applications was given by J. H. Cooper AWS, Taylor-Winfield Corp., Warren, Ohio, at the April 8th dinner meeting of the *Columbus* Section held at the Riverside Restaurant.

BONDING AND BRAZING

Dayton, Ohio.—Approximately 50 members of the *Dayton* Section and ASTE met for a joint dinner meeting at Suttmiller's Restaurant on April 11th. Speaker of the evening was Albert T. Van Cutsem, supervisor, Mechanical Assembly Group, Manufacturing Research and Development of the Glenn L. Martin Co. His subject was "Resin Bonded Aluminum and Brazed High-Temperature Sandwich Construction." A color film showing the use of structural adhesive bonding on the production of the Matador Guided Missile at the Glenn L. Martin Co. was then shown. This type of fabrication has been necessary due to the higher speeds and heats that metals are subjected to in these missiles. Adhesive bonding presents a number of advantages in that it reduces machine time required and cuts man-hours—this means fewer dies, less toolings, less skilled help required and makes improved, lower cost design possible. Originally developed to meet the aircraft industries needs for high-strength, lower-weight, easily fabricated material, this type of construction is no longer restricted to aircraft alone, according to Mr. Van Cutsem; it is being used more and more in the construction industry as well as truck, trailer and railroad car manufacture. It is becoming increasingly important in fields where a good strength to weight ratio material is a prime consideration.

Slides showing this type of sandwich construction were also used to illustrate the talks. Approximately 68 people attended the technical portion of this program.

INERT ARC WELDING

Appleton, Wis.—J. Warden Cunningham AWS, the Air Reduction Sales Co., was the guest speaker at the April 15th meeting of the *Fox Valley* Section held at the Elks Club. The speaker gave an excellent discussion on the subject "Inert-Gas-Shielded Consumable-Electrode Metal-Arc Welding."

RECTIFIER WELDERS

Kansas City, Mo.—As the *Kansas City* Section met for its regular monthly meeting at Fred Harvey's Pine Room on April 14th, one could see that there was lots of spirit in the air for the forthcoming National Spring Meeting, which will take place in Kansas City on June 7th to 10th. This enthusiasm was heightened by the attendance at this April meeting of the Exposition Manager, Bob Kenworthy, who gave a very peppy coffee talk, telling the members just what to expect in June and the part the local Section will play in making this show a success. Members of the Kansas City Section are looking forward to welcoming fellow members from all over the United States, as they feel they have a fine show planned, not only from the exposition and technical meeting standpoint, but also from the outside activities, including a gala western-style barbecue.

The main speaker of the evening was G. K. Willecke AWS, Physicist for the Miller Electric Co. of Appleton, Wis., who gave a very interesting and enlightening talk on Recent Developments in Rectifier-Type D-C Welders. It was felt that everyone present had a much better understanding of the recent developments and the parts that they play in the various welding applications.

DEMONSTRATION NIGHT

Bethlehem, Pa.—Technical innovations in welding were demonstrated at a dinner meeting of the *Lehigh Valley* Section held on Monday, April 4th, at Lehigh University. Fifty-five members and guests attended the dinner meeting at Lamberton Hall. "Demonstration Night" was held at Packard Laboratory, Lehigh. Demonstrations and exhibits included the following:

1. Linde Air Products: Oxy-

acetylene machine beveling
2. Air Reduction Sales: Oxy-acetylene flame gouging
3. Arcair Co.: Carbon arc-air gouging
4. Ind-X-Co.: Spot check X-ray

Lectures were held by:

1. Ross Peterson: Magnaflux
2. R. D. Flatland: Ind-X-Spot check X-ray
3. T. W. McFarlan of Sperry Products: Ultrasonic reflectoscope

The lecturers spoke on their respective methods of inspecting welds both in the shop and in the field.

About 160 members and guests were present.

FLAME CUTTING

Beloit, Wis.—The last monthly meeting of the *Madison* Section for the season was held on March 31st. The meeting was highlighted by a tour through the Fairbanks Morse plant, after which the regular dinner and business meeting were held at Morse Hills, the employees' club of Fairbanks Morse.

The tour was arranged by Charles Dreutzler AWS, welding engineer of the Beloit Fairbanks Morse plant. A talk was given by R. C. Watson, general manager of the plant, which emphasized the points of major interest in the plant and the diversification of the Fairbanks Morse enterprise. The tour emphasized the fabrication of Diesel locomotives and the associated welding and materials handling operations. Also, the group was shown a water pump under construction which will be the largest in the United States with a capacity of 400,000 gallons per minute.

The technical speaker for the business meeting was Mark Hippe AWS, of the Milwaukee district, Linde Air Products Co., who spoke on Flame Cutting Techniques.

The Madison Section feels that this type of meeting is of benefit to its members and that it fulfills thoroughly the objectives of the SOCIETY. Just tribute must be paid to such industries as Fairbanks Morse who recognized the value of the interchange of technical information and ideas.

Frank McGuire of Union Carbide and Carbon Corp., the dinner speaker of the evening, talked briefly on the history of precious gems and their synthesis by modern industrial means.

FLAME STRAIGHTENING

New York, N. Y.—"Flame Straightening Bent Beams" was the topic presented to the *New York* Section on April 12th. John F. Nacucchio, president of the Pioneer Welding Co., Mechanicsville, N. J., was the speaker. Mr. Nacucchio went into great detail regarding repairs of bent and distorted beams and how and where to apply the heat to various materials.

The talk was illustrated by a number of slides that gave good details of the damaged equipment and the methods by which the repairs were accomplished.

The meeting was kept at a vigorous pace by numerous questions asked by the audience.

The meeting was held at Schwartz's Restaurant, 54 Broad St., New York City, where dinner was served prior to the technical sessions.

ELECTRODE SPECIFICATIONS

Mansfield, Ohio.—The April meeting of the *North Central Ohio* Section was held at the Mansfield Leland Hotel on April 1st. Fifty-two members and guests enjoyed a movie "This Is Airpower" shown through the courtesy of the North American Aviation Co., Columbus.

Technical speaker was Ed Turnock AWS, manager of the Welding Electrode Section, Westinghouse Electric Co. Mr. Turnock outlined the work of the AWS-ASTM Committee on Filler Metal and reviewed the recently published specifications for mild and low-alloy steel electrodes. Mr. Turnock also showed movies of the welding arc taken at extremely high speed. These pictures showed the difference of metal transfer for different AWS type electrodes. High-speed pictures of the inert-arc consumable electrode process were also shown.

The Section was again honored to have present its district director, John Blankenbuehler.

STAINLESS STEELS

Knoxville, Tenn.—The *Northeast Tennessee* Section held its regular monthly meeting on March 21st at the S&W Cafeteria in Knoxville. A description of the compositions, uses and physical and mechanical properties of martensitic, ferritic and austenitic stainless steels was presented by H. F. Reid AWS of the McKay Co., York, Pa. The welding problems encountered in the joining of each type of material were described and special welding techniques that have been found useful, were discussed. It was pointed out that postweld heat treatments should be chosen which will not result in the formation of sigma phase with its consequent undesirable effects. The talk was very well received as evidenced by the many questions posed by the audience.

PRODUCT DESIGN

Erie, Pa.—The *Northwestern Pennsylvania* Section held a dinner meeting on April 20th at Soudans Restaurant. Speaker at the technical session was John Mikulak AWS, Worthington Corp., whose subject was "Product Design for Welding." Mr. Mikulak covered the following points: Cost and variables of weld application, effect of design on weld application, proper weld design, equipment and jigging for economical weld cost, and using welding for economical cast weld design. Slides were used to demonstrate some of the weldments.

The speaker also covered manual arc welding, automatic and semiautomatic submerged welding, inert-arc and gaseous-shielded arc processes, and the joint design to obtain maximum results using these processes.

ELECTION OF OFFICERS

Pittsburgh, Pa.—The *Pittsburgh* Section announces the election of the following officers for 1955–56:

Chairman—Ludwig Adams, Pittsburgh-Des Moines Co., Neville Island, Pittsburgh 25, Pa.

1st Vice-Chairman—H. L. Anthony, Mellon Institute of Industrial Research, 4400 Fifth Ave., Pittsburgh 13, Pa.

Secretary-Treasurer—J. F. Minnotte, Minnotte Brothers Co., Hays 7, Pittsburgh, Pa.

STEEL SHIP CONSTRUCTION

Portland, Ore.—The *Portland* Section was honored to have J. H. Humberstone, AWS national president, and J. G. Magrath, national secretary, as guests at the April 5th meeting held at the Heathman Hotel. Mr. Humberstone gave a very interesting talk on the aims and objectives of the SOCIETY. Mr. Magrath discussed the membership and the benefits gained by being a member of the AWS.

A. J. Bissell AWS, consulting engineer, gave a very interesting talk entitled "Common Sense Practices in Steel Ship Construction." Mr. Bissell has been connected with ship construction for many years and discussed in detail the problems encountered in welded steel ships.

UNUSUAL WELDING APPLICATIONS

Rochester, N. Y.—A regular monthly meeting of the *Rochester* Section was held on April 18th at the Liederkranz Club. A short business meeting took place before the technical session. Four Section members

Q. Isn't all filler metal for consumable electrode welding about the same?

A. No indeed—it's the quality of the filler metal that controls the quality of the weld.

Q. How can you be sure of getting the best electrode?

A. By insisting on Alcoa's new I.G.* Electrode.

Q. Why?

A. Every production lot of Alcoa® I.G. Electrode is test welded and the welds X-rayed for soundness. Each 10-lb spool is individually packed to maintain the super-smooth finish to assure soundness of weld and smoothness of feed. Each of five alloys, 1100, 4043, 5154, 5254 and 5356 (formerly 2S, 43S, A54S, B54S and XC56S), is available in six diameters. There is no better electrode.

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Your guide to aluminum value

I.G. ELECTRODE MADE OF
ALCOA ALUMINUM
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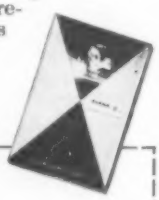

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delivered papers on unusual applications and things of interest in welding that they had seen. The four members and their subjects were as follows:

Dick Avery, the Pfaudler Co.—Stainless Steel Fluxes for Automatic Submerged Arc Welding
James Cimino, A. O. Smith Corp.—The Welding of High-Strength Aircraft Parts
William Nash, Rochester Steel Treating—Flame Hardening
William Weber, The Pfaudler Co.—Lime- vs. Titanium-Coated Stainless Electrodes

After the papers were delivered, the audience chose which one was best by ballot, the prizes for the first two being the payment of a year's dues; for the third and fourth, the payment of 1/2 year's dues.

The winners were in the following order: William Weber, James Cimino, Dick Avery and William Nash. The papers were very well prepared and proved to be very interesting and worth while to all present.

WELDING METALLURGY

Rochester, N. Y.—The March meeting of the *Rochester* Section was held on March 21st at the Liederkranz Club. The Section was fortunate in having as its speaker Dr. W. D'Orville Doty AWS, who is welding research engineer at the United States Steel Corp. His subject was Welding Metallurgy. Dr. Doty had excellent slides and was well versed on the subject and discussed the various metallurgical changes that go on in the various types of welding.

Dr. Doty is co-author of "Weldability of Steels" published by the Welding Research Council. He is a member of the Executive Committee of the Pittsburgh Section AWS, as well as a member of a number of other societies. His subject ranged from gas welding to what happens through metallic arc, submerged arc, inert arc and resistance welding.

TOOL AND DIE WELDING

Saginaw, Mich.—Sixty-three members and guests of the *Saginaw Valley* Section attended the April 14th meeting held at the High Life Inn. Results of the election of new officers for the coming year were announced as follows:

Chairman—Irving Makulinski, Buick Motors, Flint
Vice-Chairman—Bob Woollard, Dow Chemical Co., Midland
Secretary—Joe Stanulis, General Welding Works, Saginaw
Treasurer—Fred Williams, Buick Motors, Flint
Directors—
Larry Elliot, Bay Products Co., Bay City
Tom McMullen, United Welders, Bay City
Harold Kleinert, Valley Welding & Boiler Co., Bay City
R. E. Rosenbaum, General Electric Co., Saginaw

The outgoing chairman, W. E. Mochring, was presented with a Past-Chairman's Pin by the Section in appreciation for his services over the past several years.

The technical program was presented by R. K. Lee AWS, vice-president of Alloy Rods Co., York, Pa. Mr. Lee's talk was on Tool and Die Welding. He stated that the main consideration was matching the electrode to the five basic types of tool steel rather than to each individual tool steel. Mr. Lee emphasized that repairing of tool steel dies and the welding of composite tool steel dies are not production type jobs; the welding should be done slowly and carefully and the preheat and postheat treatments should conform to the type of tool steel being worked on. Each job should be considered individually and the technique considered carefully before starting the work. Mr. Lee brought out the considerable savings involved by using composite fabrication where a tool steel electrode could be used for depositing wearing or cutting edges on a base of 1040 steel.

Several questions were answered by Mr. Lee at the end of his talk indicating great interest in the subject.

SOLDERING OF CONNECTORS

Syracuse, N. Y.—The regular monthly meeting of the *Syracuse* Section was held in the Hiawatha Room of the Hotel Onondaga on April 13th. Coffee speaker was Dick Torreys, administrative assistant to the mayor of Syracuse, who spoke on the operation and problems of the city government. The talk was quite informal and a question and answer period followed which was both interesting and informative.

The speaker of the evening was George W. Cavanaugh, supervisor of the Inorganic Chemical Section, General Electric Co., Electronics Laboratory, Electronics Park, Syracuse, N. Y.

The subject of his talk was "Solderless Wrapped Connectors." Mr. Cavanaugh noted that soldering is one of the oldest metal-working processes and evidence of its use has been found as far back as 18 B.C. But little progress had been made in its applica-

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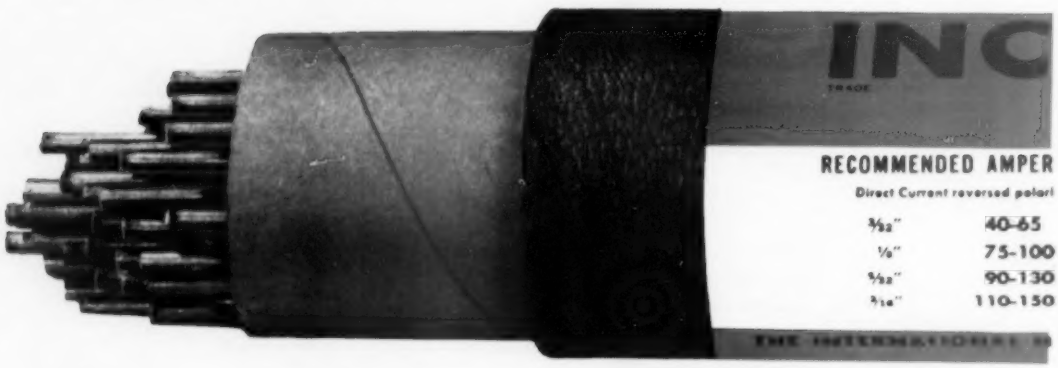

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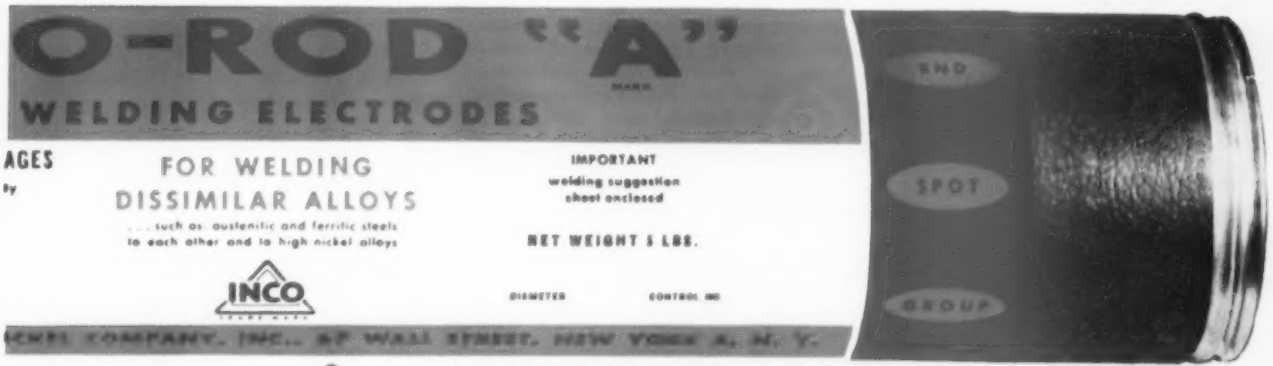

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tion until the last 10 years. The reason behind the fast growth in that period of time was probably due to the electronic industries requirements for producing good connections at high rates of production.

When the printed circuit was developed in 1950, automation in connecting of the circuits was desired and the process of solderless wrapped connectors was developed.

This process consisted of tightly wrapping the wire around the connector under tension to make a connection which is satisfactory from both an operational and mechanical standpoint. The savings resulting from using this process are considerable.

Mr. Cavanaugh noted in regard to savings that the rejects in the Potentiometer Division at Electronics Park were cut by 50% which in itself is a sizable amount.

A question and answer period followed in which it was brought out that the copper wire and connectors are tinned to prevent or minimize possible corrosion.

The only wire used thus far is single wire. The application of this process to the stranded wire has thus far proved unsatisfactory.

SIMPLIFIED METALLURGY

Baltimore, Md.—The April meeting of the *Maryland* Section, held at the Engineers Club on April 15th, had as an after-dinner feature a showing of an excellent movie "The World Series, 1954."

For the technical session, it was the Section's good fortune to have A. L. Schaeffler AWS, of the Allis-Chalmers Manfuacturing Co., who presented "Simplified Metallurgy for Welders and Supervisors." Mr. Schaeffler's very interesting and instructive talk was well illustrated with cleverly drawn colored slides, which demonstrated very clearly the benefits obtainable by preheating.

The audience was so interested in the speaker's presentation, that Mr. Schaeffler was required to devote about an hour to answer the barrage of questions that arose during the quiz period.

MILD STEEL WELDING

Miami, Fla.—The *South Florida* Section held its regular monthly meeting on April 26th in the Miami Builders Exchange Building. Speaker was W. F. Belden AWS of the Linde Air Products Co., Atlanta, Ga.

Mr. Beldon spoke on mild steel welding automatic and semiautomatic processes, ending his talk with a few slides on data charts and applications. A brief question and answer period was held after the talk.

STUDENT AWARDS MADE BY THE TUCSON SECTION

(Left) Bob Smith and Joe Brennan, vice-chairman and chairman, respectively, of the Tucson Section, looking over the welding and cutting equipment awarded to winning students. *(Right)* Scene at the actual awarding of prizes: *Left to right,* Ralph Smith, District Director, District No. 10; Clinton Clifford, 2nd place winner; Joe Brennan; Joaquin Gonzales, 1st place winner; Jack Annibal, 3rd place winner; and Hal Savage, Director-at-Large

STUDENT PRIZES

Tucson, Ariz.—The *Tucson* Section presented more than $300 worth of welding equipment, as awards, to three of Tucson high school's vocational welding classes' top welders. A Victor oxy-acetylene outfit, complete with a set of Dye Oxygen cylinders, representing first place, was awarded at the regular stated meeting on April 19th at Marco's Restaurant.

Joaquin Gonzales, top welder in Southern Arizona's vocational high-school welding classes, was presented the award by Hal Savage, director at large. Gonzales is not a novice at winning welding awards as he won a top award for welding at the recent Arizona State Fair.

Welding projects were set up by George Hunt AWS, instructor at Tucson high school's vocational welding classes, and William "Bill" Voorhees AWS, owner of Tucson Steel Fabricators, was chairman of a committee made up of owners and supervisors of Tucson's largest steel fabricating plants. Projects completed by the competing students were a welded tee-pipe joint, welded in position, by the oxy-acetylene process, a vertical butt weld by electric weld, a 3/8-in. vertical weld for a nick break test and a paper of not more than 300 words on the subject "Why I Chose Welding as a Career."

Second and third prizes were awarded to Clinton Clifford and Jack Annibal, by Ralph Smith, director of District No. 10.

Mr. Savage, in giving the award to Gonzales, stated that to his knowledge, the Tucson Section had another *first* in making the award to top welders of vocational high-school students, and that he had been informed by officers of the local section that the award was to be an annual presentation.

Louis J. Bazzetta, coordinator of industrial education, coffee speaker, remarked that in speaking for the students as well as the high-school and vocational classes, he hoped that the Tucson Section would be the first of many to recognize the vocational students for their achievements and proficiency in their work at school, paralleling the recognition now enjoyed by academic students.

William "Bill" Fischer AWS, Lincoln Electric Co., spoke on "Weld Wanderings" and illustrated his lecture with colored slides which he has taken during his years of selling welding equipment.

The equipment awarded as prizes to the three student welders was presented by the Victor Equipment Co., Dye Oxygen Co., Aweco Supply Co., Jackson Products Co. and Rankin Manufacturing Co.

RESISTANCE WELDING

Washington, D. C.—The *Washington* Section held its regular monthly dinner meeting on March 29th in Hall's Restaurant. Coffee speaker was Jim Simpson, NBW sports staff Commentator, who spoke on the ever-popular subject of baseball.

Main speaker of the evening was William J. Farrell AWS, Sciaky Bros., Inc., Chicago, Ill., who presented a semitechnical talk on the Principles of Resistance Welding.

New Members

Effective April 1, 1955

MEMBERSHIP CLASSIFICATION

A—Sustaining Member B—Member C—Associate Member D—Student Member E—Honorary Member F—Life Member

ANTHONY WAYNE

McCabe, Francis P. (C)
Smith, Merle J. (B)

BOSTON

Dyer, Theodore G. (C)
Gill, Raymon E. (C)
Mahoney, Vincent A. (C)
Mallon, Arthur H. (B)
McIntosh, John (B)
Nelson, Linton D. (C)
Porter, Russell I. (C)
Thatcher, Raynor M. (C)
Thompson, Alden W. (C)
Thompson, Joseph E. (C)
Western, Herbert W. (B)
Winters, Stanley G. (B)

CHATTANOOGA

Hardy, John A. (C)

CHICAGO

Andreasen, Harry N. (B)
Carlson, H. (B)
DeViney, A. F. (B)
Gold, Vance B. (C)
Lesniak, Ronald G. (C)

CLEVELAND

Ake, Vernon E. (C)
Bauer, Albert A. (B)
Bauer, Paul F. (B)
Dreher, William (B)
Friess, John S. (C)
Mukavetz, John M. (B)
Noda, Louis F. (B)
Popiel, Casey P. (B)
Post, Alan W. (B)
Tazewell, Lyttleton L. (B)

DAYTON

Wellemeyer, Willis F. (C)

DETROIT

Broughton, T. M. (B)
Dell, Lloyd C. (B)
Guzik, Albert E. (B)
Hamacher, J. Doyle (C)
Hurt, Melvin (C)
Karlson, Nils O. (B)
Peters, James (B)
Slicker, Melvin E. (B)
Van Loo, Gerard (B)

HARTFORD

Boccalatte, Mario L. (B)
Wooldridge, R. Gordon (B)

HOLSTON VALLEY

Luker, John G. (B)
McCaffrey, D. J. (B)
McConnell, R. E. (B)

HOUSTON

Beaty, John J. (B)
Bell, Arthur J. (B)
Birkelbach, W. H. (B)
Carson, James M. (B)
Chatelain, Noland J. (B)
Drews, Marvin A. (D)
Edwards, Vernon L. (C)
Freeman, Michael G. (B)
Funk, Ernest J. (D)
Haskins, Robert Wilson (B)
Heleman, Marvin C. (C)
Henze, Julius C. (B)
Hill, Robert Louis (C)
Keelin, Robert L. (C)
Lindberg, John H. (B)
Martindale, George D. (B)
Miller, Raymond T. (C)
Noffsker, C. J. (B)
Pearson, R. L. (B)
Phillips, Karoly B. (B)
Robinson, Harry E. (C)
Schneeman, John W. (C)
Serold, Arlon C. (B)
Smith, Curtis (B)
Smith, J. P. (B)
Smith, Leo (C)
Sullivan, Aulton R. (B)
Vick, Ira L. (B)
Vordenbaum, Herbert (B)
Wright, Roy J. (B)

INDIANA

Gilyeat, E. F., Jr. (C)

IOWA

Ackerman, K. H. (B)
Consolver, Lloyd A. (B)

KANSAS CITY

Corriston, E. W. (B)

LONG BEACH

Edell, Joseph E. (C)

LONG ISLAND

Biedermann, Kenneth P. (C)
Leonard, Dan (C)
Phillips, Gordon L. (B)
West, Harold (C)

LOS ANGELES

Hand, Raymon E. (B)
Kayes, Charles R. (C)
Pouliot, Walter G. (C)
Schumacher, Gerald Henry (C)
Thompson, Courtney A. (C)
Waters, Robert E. (C)

LOUISVILLE

Ditto, Robert (C)

MAHONING VALLEY

Bloom, William P. (C)
Flick, Richard L. (B)
Keiser, Leo (C)
Strong, Thomas W. (C)

MARYLAND

Burrows, Charles F. (B)
Siltanen, James N. (C)

MICHIANA

Klaiber, Henry (C)
Nicodemus, V. P. (C)

MILWAUKEE

Cooke, Harry C. (C)

NEBRASKA

Reutzel, R. E. (B)
Teter, M. D. (B)

NEW JERSEY

Boresta, John (C)
Everett, Herbert E. (B)
Finnerty, Maurice G. (B)
Goetz, George (B)
Hill, Clifford W. (C)
Luhrs, C. H. (B)
Minichiello, Richard (B)
Motsek, Michael (C)
Murchie, Donald J. (C)
Ristav, Christian (B)
Tomlinson, Robert (B)

MADISON

Chapman, Albert William (B)

NEW ORLEANS

Black, Leonard R. (B)

NEW YORK

de Altamer, Antonio (B)
Kalish, Herbert S. (C)
Lord, William D. (B)

NIAGARA FRONTIER

Kagawa, Tatsuo Fred (B)

NORTH CENTRAL OHIO

Edwards, Walter R. (C)
Goodwine, Paul (C)
Greetham, Donald F. (C)
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Lee, James A. (C)
Marquis, W. R. (C)
Wilson, Ellis C. (C)
Witt, Mahlon E. (B)

NORTHEAST TENNESSEE

Claiborne, Henry C. (B)
Cook, Sam R. (B)

NORTHERN NEW YORK

Lemmo, James D. (C)
Marshall, Gordon R. (C)

NORTHWEST

Deyak, Edward C. (B)
Halverson, Paul L. (C)
Snyder, Raymond A. (B)

NORTHWESTERN PENNSYLVANIA

Coombs, Verner D. (C)
Erzer, John K. (C)
Greene, Edward E. (C)
Newman, A. J. (B)

PHILADELPHIA

Albright, Nelson (C)
Tompkins, Richard J. (C)

PITTSBURGH

Bell, Blaine (C)
Hileman, Robert E. (B)

PUGET SOUND

Ryan, J. D. (C)
Unrath, Ray (B)

RICHMOND

James, Lewis (C)

SAGINAW VALLEY

D'Alessio, Tony (D)
Kolb, Albert W. Jr. (D)

ST. LOUIS

Dudley, Jack D. (C)
Ryan, Thomas J. Jr. (C)
Taylor, Harry R. (C)
Willers, Henry W. (C)

SALT LAKE CITY

Hopkins, Charles (B)

SAN FRANCISCO

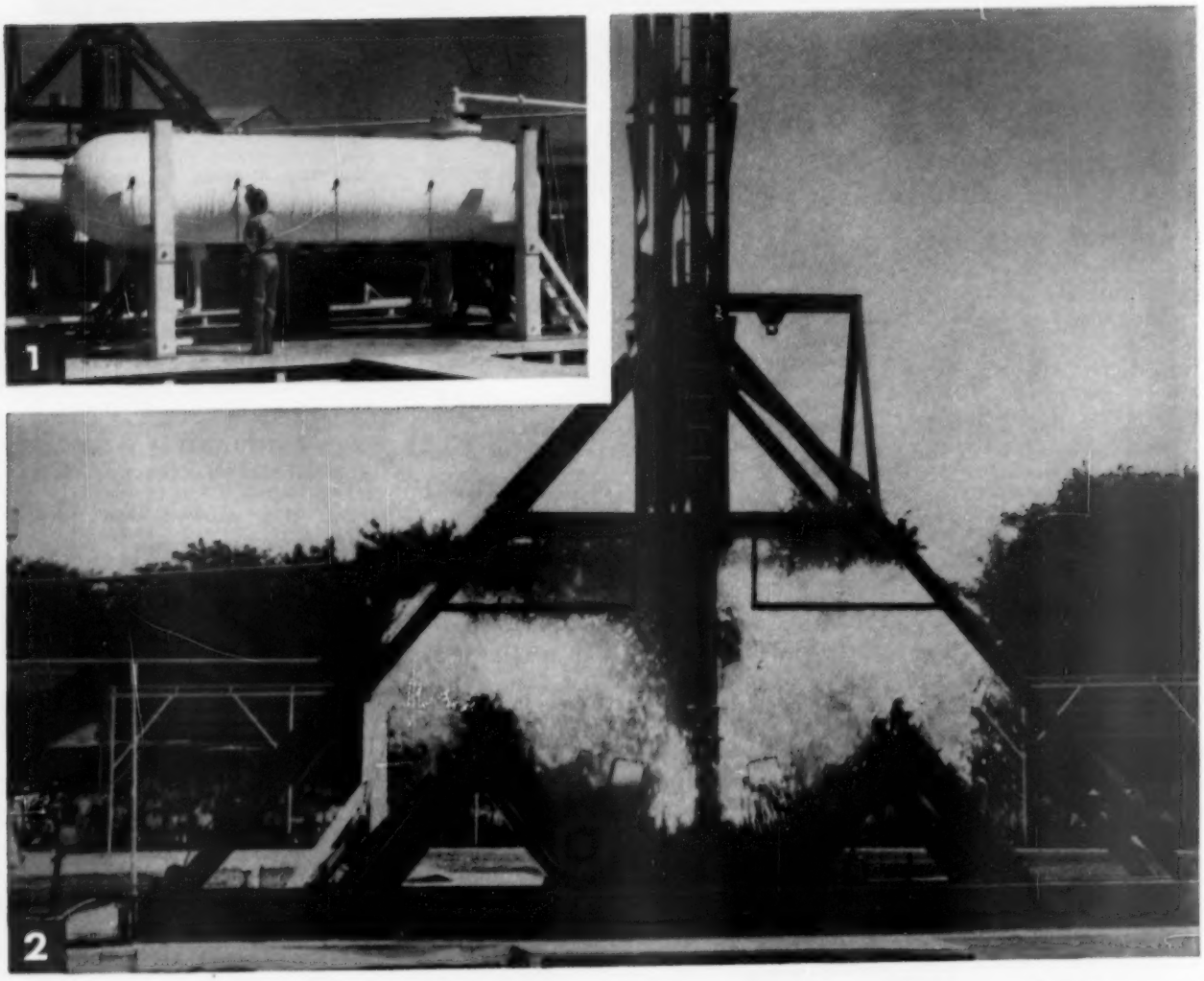
Bowden, Donald E. (D)
Lease, John H. (C)
Mikels, Ernest P. (C)

SANTA CLARA VALLEY

Belding, Richard L. (C)
Engstrom, Carl B. (C)
Grimm, O. K. (B)
Hills, Donald W. (B)

FROZEN STEEL

1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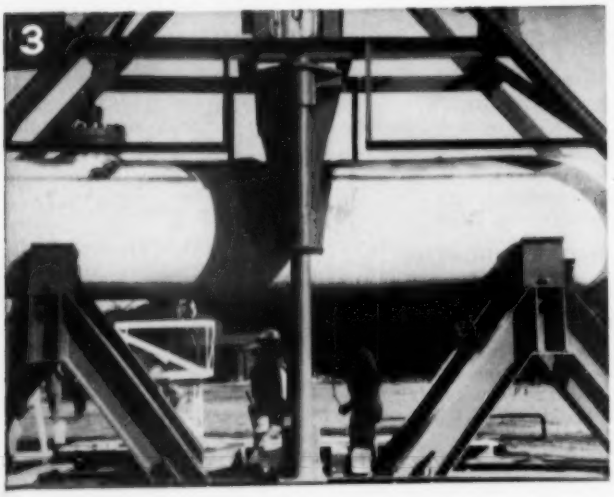
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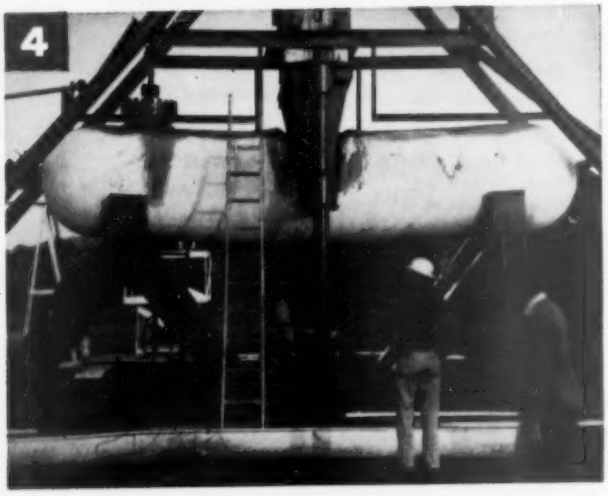
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A 13-ton steel ingot was raised high above the vessel . . . then dropped, repeatedly, until the vessel failed. The first drop from 52 feet knocked off the frost (Photo #2), but that's all. The steel and every weld remained intact, and the top of the vessel was barely dented. (Photo #3)

The second drop from 73 feet socked the vessel with an impact of 2,000,000 ft. lbs. at a temperature as low as –38° F. But the blow merely increased the dent another fraction of an inch. The steel held and the vessel was still sound. (Photo #4)

The third and final drop from 101 feet caused failure. With the 13-ton ingot striking at a speed of 55 miles per hour, the steel *had* to give. But it *didn't shatter* or show any sign of brittleness. It stayed tough and ductile. (Photo #5)

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Chevalier, Al (C)
Hochstettler, Dean J. (C)
McCullough, L. W. (B)

Sapp, James R. (B)
Secor, George B. (C)

TULSA

Kretchmar, Harold R. (B)

WASHINGTON, D. C.

Dudzinski, Matthew (C)
Echols, Elmo G. (C)
McKenzie, J. W. (C)
Norris, Samuel C. (C)

WESTERN MASSACHUSETTS

Merriam, William A. (B)

WESTERN MICHIGAN

Johnston, David K. (B)

YORK-CENTRAL PENNSYLVANIA

Beittenmiller, J. Warren (B)
Milsap, Roger (B)
Seifried, Kent B. (B)

NOT IN SECTIONS

Leonard, Jean-Baptiste (C)
Beever, Eric Weston (C)
Crocco, Francis B. (B)
Felker, Vance R. (B)
Vanbiesem, Jorge (B)

●

Members Reclassified

During the month of April

COLORADO

Waugaman, John A. (C to B)

LONG ISLAND

Biederman, Harry (C to B)

LOS ANGELES

Powell, William T. (C to B)

MARYLAND

Frost, Marion Edward (C to B)

NEW YORK

Bell, Mace H. (C to B)

RICHMOND

Baker, J. H. (B to A)

SAINT LOUIS

Fitzhugh, Elves (C to B)

ABSTRACTS OF CURRENT PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,706,231—Apparatus for Producing Discontinuous Conductors—Alfred W. Tyler, Anoka, Minn., and Chapin Wallour, Wellesley Hills, Mass., assignors to the United States of America as represented by the Secretary of the Navy.

This patent relates to a specialized apparatus for automatically producing a series of successive predetermined lengths of conductive material on a continuous length of insulating supporting core. The conducting material originally has a continuous length and a pair of high current contacts are provided for contacting the conducting material to pass a fusing current through such connecting material at predetermined intervals spaced along its length. Such current produces breaks in the continuity of the connecting material at the portions thereof contacted by the contacts.

2,706,232—Combination Jig and Welding Apparatus—Frank John Pilas, Kearny, N. J., assignor to Radio Corporation of America, a corporation of Delaware. (Application Dec. 5, 1951).

Apparatus for assembling a mount for an electron discharge device made from a tubular cathode, an anode and a plurality of other electrodes is shown in this patent. The apparatus includes a jig having a supporting surface with a plurality of recesses for receiving portions of the discharge device therein. Movable electrodes are supported adjacent the jig and can be moved into engagement with members positioned thereon for welding action.

2,706,233—Welding Electrodes for High Frequency Electric Sewing Machines—Sakuji Yamaguchi, Nagoya City, Japan.

Welding electrodes for high-frequency electric sewing machines are disclosed in this patent. The electrodes comprise an upper solid metallic roller electrode to be used in place of the needle operating mechanism on the sewing machine and is movable up and down. A lower roller electrode is driven by the driving shaft of the machine and it has a closely coiled spiral spring fitted around an insulator disk to provide an elastic deformable working periphery on such lower electrode.

2,706,235—Metal Cutting by Means of Electric Arc—George G. Landis, South Euclid, Ohio, and John M. Parks, La Grange, and John E. Forss, Chicago, Ill., assignors to the Lincoln Electric Co., Cleveland, Ohio, a corporation of Ohio.

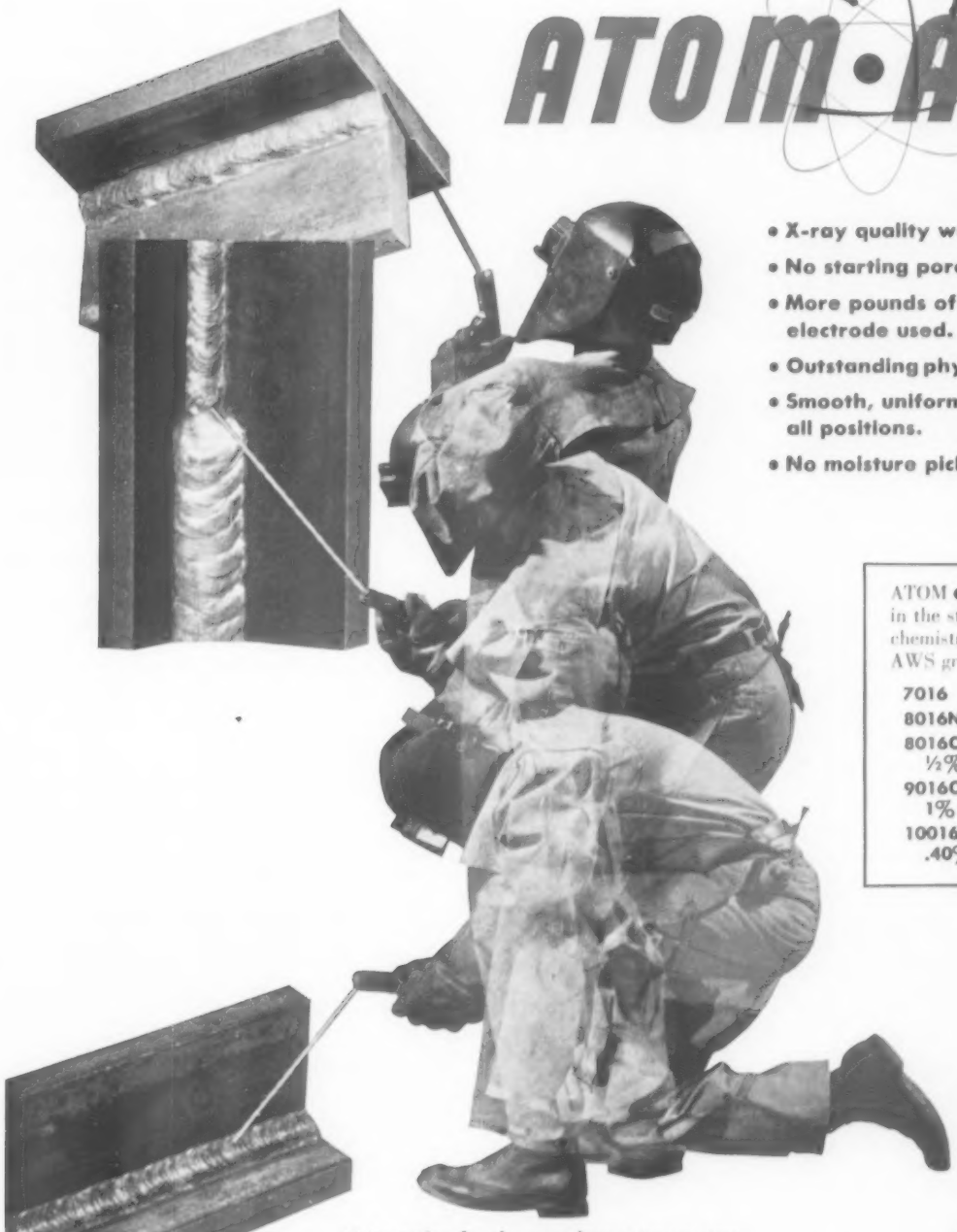
The patented metal cutting method comprises moving a consumable electrode until a side of it spaced from the end thereof contacts the workpiece and an arc is struck between such side and the workpiece and, thereafter the electrode is moved sidewardly in the desired direction of the cut at a rate sufficient to maintain the arc between the forward side only of the electrode and the apex of the cut to melt away the apex in advance of the electrode. Simultaneously the electrode is fed lengthwise fast enough to maintain the electrode within the cut to substantially the full depth thereof. The electrode is continuously melted and the heated electrode metal is carried to the apex and assists the arc in the cutting action.

2,706,236—Cutting and Gouging Electrode Holder and Method—Myron D. Stepath, Lancaster, Ohio, and Neil J. Krumm, Bremerton, and Cornelius A. Ross, near Bremerton, Wash., assignors to Arcair Co., Bremerton, Wash., a partnership.

This patent relates to a method of gouging metal by use of a consumable electrode. The method specifically comprises positioning an electrode with a portion of its length projecting toward the work, and heating and melting the portion of the work to be gouged by striking and maintaining an arc between the projecting end of the electrode and the work. This causes consumption of the projecting portion of the electrode progressively. Such heating and melting of the work is continued, and the melted metal is forcibly blown from the work beneath the arc by a stream of air. This air stream effectively removes the melted metal from beneath the arc and effects a uniform gouging action as the projecting portion of the electrode is consumed.

the only all position iron powder low hydrogen electrode...

ATOM•ARC



- X-ray quality weld metal.
- No starting porosity.
- More pounds of weld metal per electrode used.
- Outstanding physical properties.
- Smooth, uniform weld bead in all positions.
- No moisture pick up.

ATOM ● ARC is available in the strength levels and chemistry of the following AWS grades:

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8016N (3½% Ni.)
8016CM (1¼% Cr., ½% Mo.)
9016CM (2¼% Cr., 1% Mo.)
10016MM (1.70% Mn., .40% Mo.)

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Pacific Coast Sales Offices and Plant
750 Lairport Street
EL SEGUNDO, CALIFORNIA

Alloy Rods Co.

NEWS of The Industry

IAA Convention Round-Up

The International Acetylene Assn.'s 55th Annual Convention, held in Houston, March 22nd, 23rd and 24th, was a most successful and outstanding event.

The opening luncheon on March 22 was attended by 380 Association members and their guests. J. Carl Bode, president, National Carbide Co., and the vice-president of the Association, was Chairman of the meeting.

The Honorable Roy F. Hofheinz, Mayor of Houston, welcomed the Association to Houston in true Texas fashion. Following this, President E. A. Daniels spoke about the future of the industry and some of the problems that will have to be solved.

John G. Forrest, Business and Financial Editor of the *New York Times*, one of the nation's top writers and editors in the business and financial field, was the guest speaker at the opening luncheon. He spoke on the subject, "What's Ahead for American Business."

One of the outstanding events of the Houston meeting was the award of the James Turner Morehead Medal for 1954 to Dr. Augustus B. Kinzel, vice president—research, Union Carbide and Carbon Corp., at a reception and dinner on Wednesday evening, March 23rd. Approximately 210 members of the Association and their guests were present to see Dr. Kinzel receive the medal for "his scientific and technological contributions to acetylene utilization."

At the Annual Business Meeting on March 24th, Mr. Bode was elected president. C. McL. Pitts of the People's Gas Supply Co., Ltd., Ottawa, was elected vice - president. Walter Bender of Shawinigan Products Corp., New York, was elected treasurer and H. F. Reinhard of Union Carbide and Carbon Corp., New York, was elected Secretary.

Following the Business Meeting, there was a member technical session at which C. G. Andrews, Air Reduction Sales Co., was chairman, and A. C. Mattison, Linde Air Products Co., H. E. Lorenz, Independent Engineering Co., A. R. Hutt, Air Reduction Co., and F. R. Fetherston, Compressed Gas Assn., were the principal contributors.

Also on Thursday, March 24th, about 100 members of the Association and their guests attended a demonstration by G. M. Kintz and H. F. Browne, Accident Prevention and Health Division, Bureau of Mines, U. S. Department of Interior, Dallas. The "Magic of Fire," a demonstration of fire and explosive mixtures of gases, was especially interesting to all members.

Servel Appoints Morrill

John R. Morrill, of Spokane, Wash., has been elected a vice-president of Servel, Inc., and on June 1st will become general manager of the company's commercial refrigeration division.

Announcement of Mr. Morrill's appointment was made recently by Duncan C. Menzies, Servel president.

John R. Morrill

For the past three years Mr. Morrill has been vice-president and general manager of Gibson Welding Supplies, of Spokane, manufacturers and distributors of welding equipment and supplies.

Previously he was vice-president of the Baker Raulang Co., of Cleveland, manufacturers of industrial fork lift trucks, and assistant vice-president of the Lincoln Electric Co. of Cleveland.

As vice-president and general manager of Servel's commercial refrigeration division, Mr. Morrill will be completely responsible for that division's engineering, procurement, manufacturing, distribution, sales, service and profit-and loss.

Mr. Morrill was born in Cleveland, and educated in Columbus. He was graduated from Stanford University with a B.A. degree in engineering, and took graduate work in the Stanford University School of Business. He was elected to Phi Beta Kappa, honorary scholastic fraternity, and to Tau Beta Pi, honorary engineering fraternity. He is a member of the AMERICAN WELDING SOCIETY.

Taylor-Winfield Host to Engineering Students

Twelve upper class students of the Welding Engineering Department of Ohio State University recently visited the Taylor - Winfield manufacturing plant to learn firsthand about plant productions and layout and, specifically, resistance welding techniques.

Carbide Production Film

A new 16-mm sound-and-color film, "Fiery Magic," has been released by the National Carbide Co., a Division of Air Reduction Co., Inc.

The title of the film refers to the blazing electric furnaces where coke and lime are combined to form calcium carbide.

Consecutive steps in the manufacture of carbide are clearly demonstrated by an animated flow chart, while the colorful sights of the process are reproduced on footage shot at various National Carbide plants. Running time of the film is about 23 min.

"Fiery Magic" also portrays the varied uses of acetylene—from its early use in lamps, through the rise of oxy-acetylene welding and cutting, and finally as an important base in modern chemical processes.

"Fiery Magic" may be borrowed for showings from district offices of the Air Reduction Sales Co., National Carbide outlets or directly from National Carbide Co., 60 E. 42nd St., New York 17, N. Y.

Ampco Distributors

Ampco Metal, Inc., Milwaukee, Wis., has announced the appointment of several exclusive distributors in various sections of the country.

These distributors are the Pacific Metals Co., Ltd., with offices in San Francisco and Los Angeles, Calif.; McDonald & Wilson Sales Co., St. Louis, Mo.; Miller Equipment Co., Cincinnati, Ohio; the Red Arrow Sales Corp. in Madison, Wis.; and Arizona Welding Equipment Co., Flagstaff, Ariz.

As exclusive distributors for Ampco bronze welding rod, each will serve their respective trading areas by carrying a full line of Ampco products.

The General Distributing Co., Great Falls, Mont., has been appointed as an exclusive distributor for the Ampcoweld line of resistance welding electrodes and alloys for the state of Montana.

Nelson Appointments

Appointment of George Medsker as field engineer for the Nelson Stud Welding Division of Gregory Industries in the northeastern Ohio territory, with headquarters in Cleveland, has been announced by Leonard C. Barr, vice-president and general sales manager.

Mr. Medsker joined the Nelson organization in 1952 as a construction industry specialist, after two years in sales work with Reynolds Metals building products division in Cleveland. He is a graduate of Ohio State University and lives in Elyria.

George E. Gregory, Jr., who has been field engineer in the Cleveland territory for the past two years, has been transferred to company headquarters in Lorain. He is a member of the AMERICAN WELDING SOCIETY.

GE Appoints Hopper

Douglas A. Hopper has been named general manager of General Electric's Welding Department at York, Pa., Harold E. Strang, vice-president and general manager of the company's Measurements and Industrial Products Division, announced recently.

Formerly manager of GE's Warehousing and Field Services, Large Lamp Department, Nela Park, Cleveland, Ohio, Mr. Hopper succeeds Raymond C. Freeman, who has been appointed manager, Engineering and Operations, Atomic Power Equipment Department, at Schenectady. Mr. Hopper will assume his new duties on April 18th.

A native of Minneapolis, Minn., and a graduate of the University of Minnesota, Mr. Hopper joined General Electric at Schenectady in 1933 on the business training course. Following service as a disbursement clerk, he became a traveling auditor in 1935 and, after service in other responsibilities, transferred to Newark, N. J., as manager of the Service District Lamp Division in 1943.

Mr. Hopper became manager of the Service District Lamp Division at Dallas, Tex., in 1944. After a return to similar duties at Newark the following year, he was named to the Nela Park position.

Mr. Hopper has resided in Cleveland with his wife and two children.

CHAMPION
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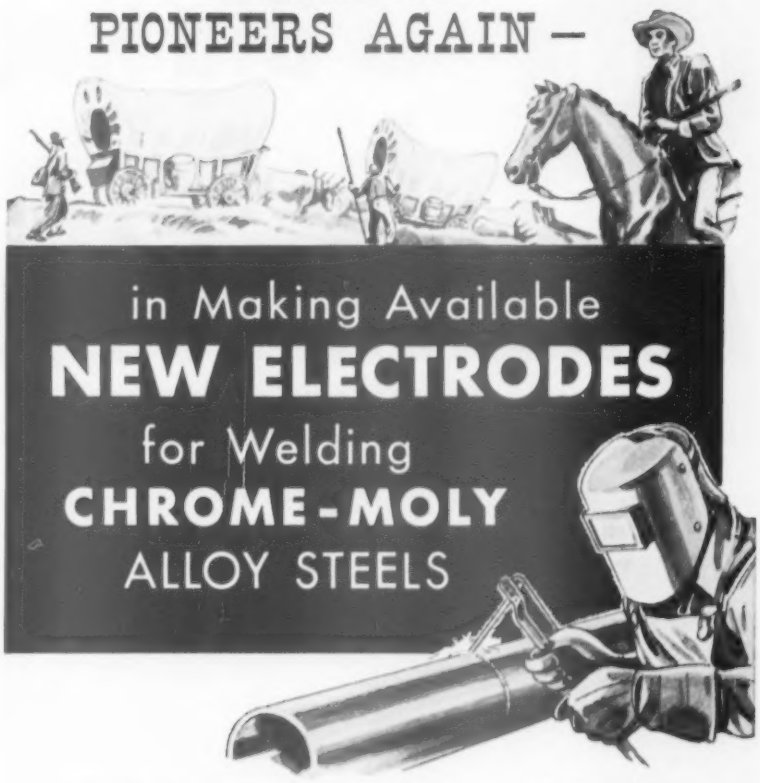

in Making Available
NEW ELECTRODES
for Welding
CHROME-MOLY
ALLOY STEELS

Champion now brings to the fabricator of Chrome-Moly Steels a new type of low hydrogen electrode of the AWS xx15 type, excellent for all position welding.

Champion's Croloy Electrodes produce a weld deposit with a carefully controlled composition which almost completely eliminates weld cracking and greatly reduces the preheat necessary as well as relieving the necessity of maintenance of preheat prior to stress relieving.

Champion Croloy Electrodes produce very smooth weld deposits in all positions and pass the most rigid X-ray requirements. Weld deposits from Croloy Electrodes possess very high ductility, 20-26% in the as-welded condition, and 24-30% stress relieved at 1350°F, as well as show stress-to-rupture properties equal to or exceeding that of the plate material. Champion Croloy Electrodes 3/32" through 3/16" diameters are available in grades for welding .5% Cr, .5% Mo through 5% Cr, .5% Mo material.

Complete technical data on Champion Croloy Electrodes will be furnished upon your letterhead request. Write Now! Dept. W.

THE CHAMPION
RIVET COMPANY
CLEVELAND 5, OHIO — EAST CHICAGO, INDIANA

ACCO products

3 Big Reasons for Buying PAGE Automatic Welding Wire

1. Packaged to fit your particular requirements

2. A wide range of analyses to fit a particular job

3. Greatest convenience — you can buy from local stocks

There is every reason why you should look to PAGE for your automatic welding wire needs. For PAGE not only manufactures wire in an extremely wide range of analyses, but packages them in a variety of ways for the most complete protection and convenience in handling and stocking. PAGE Distributors throughout the country carry stocks — this means savings for you, in ordering time and in inventory investment.

Shown above are some of the handy ways PAGE Automatic Welding Wire is packaged...There are lightweight, durable Leverpaks, which provide perfect protection against coil distortion or wire corrosion. They're easily opened and resealed, roll easily, stack perfectly and take a minimum of floor space.

There are single and pallet-mounted (1,000 lb., 2,000 lb., 3,000 lb.) coils, each wrapped in waterproofed paper and held secure by steel strapping; thread-wound, 25-pound reels to fit popular machines; also, coils in individual cardboard cartons, available singly or palletized for handling by fork-lift truck.

Page Offers 26 Analyses in Automatic Welding Wire

In order to provide you with *exactly* the correct automatic welding wire for your specific requirements—no matter how "special" or "different" they may be—PAGE offers no less than 26 different analyses from which to choose. These "cover the waterfront" of applications: heavy automatic submerged arc...light manual submerged arc ...inert gas manual and automatic, tungsten or metal arc.

Carbon Steel • Any carbon from Armco (.025 max.) to high carbon (.90—1.10). All standard AISI analyses in between.

Low Alloys • All the most popular welding grades

Stainless • AISI grade. Other types on request

Following is a list of the *26 analyses* in which PAGE Automatic Welding Wire is readily available:

Page A-S-Armco	Page A-S-110	Page NAX-9115
Page A-S-6	Page A-S-15	Page-Allegheny A-S-Type 304
Page A-S-10	Page A-S-15-MO	Page-Allegheny A-S-Type 308
Page A-S-17	Page A-S-3½-Nickel	Page-Allegheny A-S-Type 308 ELC
Page A-S-20	Page A-S-6150	Page-Allegheny A-S-Type 309
Page A-S-65	Page A-S-4130	Page-Allegheny A-S-Type 310
		Page-Allegheny A-S-Type 316
		Page-Allegheny A-S-Type 316 ELC
		Page-Allegheny A-S-Type 347
		Page-Allegheny A-S-Type 348
		Page-Allegheny A-S-Type 410
		Page-Allegheny A-S-Type 420
		Page-Allegheny A-S-Type 430
		Page-Allegheny A-S-Type 502

Details of all these analyses are contained in our new Folder DH-402. Write for it!

from Page's Wide Range You Can Get Exactly the Analysis You Need for Each Welding Job

• The *right* electrodes and the *right* rods for every welding job! That is what you must have for perfect operation—and that is what you can expect from the wide range offered by PAGE. Choose what you need from this complete line:

Gas Welding Rods

Armco Gas (GB-45)	Page Armco
Mild Steel (GA-50)	C Gas
Low Alloy (GA & GB-60)	Hi-Tensile M
3½% Nickel (GB-65)	3½% Nickel
.55—.65 Carbon	Medium Carbon
.90—1.10 Carbon	High Carbon
Manganese Bronze	Manganese Bronze
Naval Bronze	Naval Bronze
Stainless Steel	According to AWS analysis

Any Page A-S Automatic Grade in 36" Lengths

Bare Electrodes

Armco	Page Armco
Low Carbon	Page "CE"
.13—.18 Carbon	Page "B"
.55—.65 Carbon	Medium Carbon
.90—1.10 Carbon	High Carbon

Metal Spray Wire

Armco	Page Armco
.10 Carbon	#10
.28 Carbon	#25
.40 Carbon	#40
.60 Carbon	#60
.80 Carbon	#80
1.00 Carbon	#100
3½% Nickel	3½% Nickel
Manganese Bronze	Manganese Bronze
Naval Bronze	Naval Bronze
Stainless Steel Metal Spray	According to AWS Analysis

Convenient Service from Local Stocks

• Whenever you are in need of automatic welding wire, electrodes or welding rods, you can get them quickly and easily from your nearby PAGE Distributor—no matter what part of the country you live in.

PAGE Distributors carry ample stocks from which your requirements can be filled without delay or inconvenience to you. This handy service not only saves you time in getting what you need, but makes it unnecessary for you to make a sizeable investment in inventory. Thus, your nearby PAGE Distributor is able to make double savings for you—a saving in time and a saving in actual dollars.

Get the full Analysis List

All the 26 analyses of PAGE Automatic Welding Wire are set forth in complete detail in new **Folder DH-402.** A free copy of this informative folder will be sent to you on request. Write to our Monessen, Pa., office.

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Page Steel and Wire Division
AMERICAN CHAIN & CABLE

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THE MOST ADVANCED
AUTOMATIC FLUXING SYSTEM
EVER OFFERED
FOR BRAZING

EASY WAY TO LOW COST

• All-State "Jet Flux" Auto-Dispenser No. 1 as you get it from the factory . . . is a pressure cylinder complete with automatic dispenser, controls, and hose with connections . . . ready to tie in, without special tools, to single or multiple brazing stations.

• Only one supply item—All-State "Jet Flux"—fluxes brazing work automatically through the fuel gas.

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WELDING ALLOYS CO., INC.
White Plains, N. Y.

Lincoln Appoints Chief Engineers

The appointment of two chief engineers for the Lincoln Electric Co. has been announced by L. K. Stringham, engineering vice-president.

Norman J. Hoenie has been appointed chief engineer, Machine Division, and Emmett Smith, chief engineer, Electrode Division. Both men, prior to their appointment, have been assistant chief engineer of their respective divisions.

Norman J. Hoenie

Mr. Hoenie joined the Lincoln Engineering Department following his graduation from the Ohio State University with an electrical engineering degree in 1931. He is a member of the American Welding Society, American Institute of Electrical Engineers and the honorary societies, Eta Kappa Nu, Tau Beta Pi and Sigma Xi. He lives at 1031 Allston Road, Cleveland Heights, Ohio.

Emmett Smith

Mr. Smith graduated from the Ohio State University in 1928 as an electrical engineer and immediately joined the Lincoln Engineering Department. He is a member of the American Welding Society, the American Society for Metals and Eta Kappa Nu. He lives at 3264 Rumson Road, Cleveland Heights, Ohio.

•

South Dakota Tech Honors Hoyt

On April 8, 1955, Dr. Samuel Leslie Hoyt, consultant for Battelle Memorial Institute, Columbus, Ohio, was granted a doctor of engineering degree by South Dakota School of Mines and Technology in recognition of his outstanding contributions to engineering and science.

Dr. Samuel Leslie Hoyt *(center)* receiving a Doctor of Science degree from the South Dakota School of Mines and Technology, Rapid City, at Honors Day activities April 8th. Dr. Paul Anderson, head of Tech's metallurgy department, is at left; President F. L. Partlo is at right

Dr. Hoyt, from 1913 to 1919 was on the faculty of the University of Minnesota where he founded the department of metallography. He was with the General Electric Co. for 12 years, he served first as director of the Metallurgy Laboratory of the Lamp Division and later as research metallurgist. In 1931 he became director of metallurgical research with A. O. Smith Corp., and in 1939 became Technical Advisor of Battelle Memorial Institute. He was graduated from University of Minnesota, completing graduate work at Charlottenburg and Columbia universities.

He is a member of the American Welding Society, American Society for Metals, the American Institute of Mining and Metallurgical Engineers, the Institute of Metals and the American Association for the Advancement of Science.

PERSONNEL

Moody Completes 30 Years of Service

Elliot E. Moody, 43 Balmier Parkway, Cranford, a group head in the Mechanical Division of Esso Engineering, has completed 30 years of service, according to an announcement by the Esso Research and Engineering Co.

Mr. Moody began his long service in 1925 as a civil engineer with Standard Oil Company of New Jersey. In the late twenties, he had a two-year assignment with the Hydro-Engineering and Chemical Co., a Jersey Standard affiliate. He became a member of Esso Research and Engineering in 1931 as a civil engineer, and later was named a designer. Mr. Moody has been in his present post as a group head in the Inspection Section since 1953.

A surveyor before joining the company, Mr. Moody was born in Bayonne. He holds a bachelor of science degree in civil engineering from the University of Michigan, and is a member of the AMERICAN WELDING SOCIETY.

Warde Plans South American Survey

C. S. Rutherford, president, R&R Welding Supply Co., Des Moines, Iowa, has announced the resignation of Robert D. Warde, vice-president and sales manager.

Mr. Warde has been associated with R&R Welding Supply Company for the past three years. Prior to this he spent 15 years with the Linde Air Products Co. He is currently first vice-chairman of the Iowa Section of the AWS.

Robert D. Warde

Mr. Warde is moving to Delray Beach Fla., where he will headquarter while traveling to several South American countries, as well as the industrial centers of the West Indies, in the interest of a personal survey of the welding supply market in these areas.

Kinzel Promoted

Dr. Augustus B. Kinzel has been elected vice - president—research of Union Carbide and Carbon Corp., succeeding Dr. George O. Curme, Jr., according to an announcement by Morse G. Dial, president of the corporation. Dr. Curme, who is retiring as vice-president of the corporation, will continue as a director.

Augustus B. Kinzel

Dr. Kinzel has been actively engaged in research work with Union Carbide and Carbon Corp. since 1926, when he joined Electro Metallurgical Co. as a research metallurgist. He became metallurgist of the Laboratories in 1931, and a vice-president of Electro Metallurgical Co. in 1944. He was appointed director of research of Union Carbide and Carbon Corp. in July 1954.

Executive Advisor

Merritt L. Smith has been appointed executive advisor of Metal & Thermit Corp., producers of metals, industrial chemicals and welding equipment and supplies, it was announced by H. E. Martin, president. As staff coordinator, Mr. Smith will assist the president in integrating the company's varied sales activities.

Mr. Smith's experience covers more than twenty years in sales administrative work concurrent with advertising. He was for four years assistant general sales manager, and prior to his present appointment was manager of advertising and sales promotion.

Merritt L. Smith

Mr. Smith attended Dartmouth College and Columbia University, graduating from the latter in 1924 with a B.A. degree. He later returned for postgraduate work in advertising and marketing.

He is a member of the Industrial Advertisers Assn. and the Sales Executives Club, and is a member of the Manufacturers Committee of the AMERICAN WELDING SOCIETY.

Stringham Appointed Vice-President

The Board of Directors of the Lincoln Electric Co. has announced the appointment of L. K. Stringham as vice-president in charge of engineering. He has been chief engineer for the company since 1951. George Landis who has been the engineering vice-president was appointed vice-president in charge of research.

Mr. Stringham joined the Lincoln Engineering Department in 1933 immediately following his graduation from Cornell University with a degree in electrical engineering. His experience in the department he now heads has included all phases of engineering:

WELDING ARCS NEWS

COMPLETE G-E LINE OF EQUIPMENT AND ELECTRODES ASSURES YOU UNBIASED APPLICATION HELP

G-E RECTIFIER WELDERS—300 and 400 amps. Because General Electric offers all three—rectifier, a-c, **and** m-g welders, we can impartially help you choose the unit your job needs.

G-E MG WELDERS—200, 300, and 400 amps: provide unique magnetic interactor to minimize popouts and assure a steady, forceful arc.

Tests show how G-E rectifier welders provide a stable, easily handled arc, even on difficult vertical welding

FULL-TIME arc-force control virtually eliminates short-arc sticking

Faster, easier welding and improved arc stability . . . these are two important advantages you get with the new General Electric rectifier welder. And with G-E **full-time** arc-force control, you can match your job with a soft or deep-digging arc . . . at the flick of a switch.

Soft arcs are desirable for many jobs. But short-arc work needs a forceful and deep-penetrating arc.

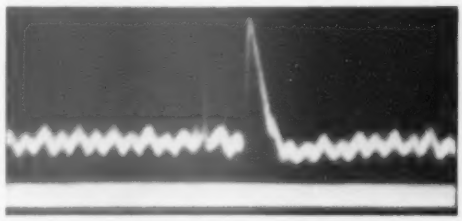
WHY YOU NEED arc-force control is shown in G-E laboratory research: As you weld, metal drops pass from the electrode to the work — causing frequent short circuits. Without arc force, the drops cool, and if you are using a short arc — the electrode will likely freeze to the job.

OSCILLOGRAPHIC PROOF (next column) shows how G-E arc-force control provides a powerful, split-second surge of current to blast the metal drops away — allowing you to bear down on the arc. Without arc force (lower graph), short circuits are long and at low amperage. The drops cool, causing sticking in short-arc welding.

CONTINUOUS OPERATION is an exclusive feature of G-E arc-force control. Since the capacitor design is self-protecting, there is no thermostat to cut out the arc force just when you need it most. G-E **full-time** arc-force is always ready to stabilize the arc and give deep penetration.

OTHER EXCLUSIVE BENEFITS of G-E rectifier welders are: combination cleaning-cooling fan; special coil mounting, which reduces wear, noise; and easily removable side covers. The efficient moving-coil design is used. Send coupon for free book.

YOUR LOCAL G-E WELDING DISTRIBUTOR IS LISTED IN YOUR PHONE BOOK UNDER "WELDING EQUIPMENT, GENERAL ELECTRIC COMPANY."



G-E arc-force blasts away short circuit

Lengthy short circuit without arc force

G-E AC WELDERS—295, 300, 400, and 500 amps: offer efficient moving-coil design; fine accuracy of current settings; simple rugged construction.

FREE SELECTOR CHART (send coupon) gives you a handy reference on full electrode application data. Complete G-E electrode line features balanced design—for best over-all results.

Section 11-1, General Electric Co., Schenectady, New York.

Please send me the bulletins checked below:

- ☐ GEA-6242, G-E Rectifier Welders
- ☐ GEA-6243, G-E AC Welders
- ☐ GEC-664, G-E MG Welders
- ☐ GEC-657, Electrode Selector Chart

Name .....

Title ..... Co

Street .....

City ..... State .....

GENERAL ELECTRIC

L. K. Stringham

experimental research, product development, testing and application.

He is a member of the Board of Directors of the company and a member of the AWS-ASTM Committee for Filler Metal. He is a Fellow in the American Institute of Electrical Engineers, a member of the AIEE Electric Welding Committee, a member of the AMERICAN WELDING SOCIETY, and the electrical honorary society, Eta Kappa Nu. He lives at 2700 Endicott Road, Shaker Heights, Ohio.

Aborn Promoted

Robert H. Aborn, assistant director of United States Steel Corp. Fundamental Research Laboratory, Kearny, N. J., since 1947, is now director of the laboratory. Dr. Aborn's undergraduate education was in liberal arts, with graduate work in metallurgy leading to the Sc.D. degree from Massachusetts Institute of Technology in 1925.

Robert H. Aborn

His career in the steel industry started in the blast furnace department of Bethlehem Steel Co. in 1920. As his interest turned to metallurgical research, he joined the staff at Watertown Arsenal, and later was associated with the research laboratory of applied chemistry at MIT. After two years in teaching and research at Harvard University, he joined the staff of the Fundamental Research Laboratory of U. S. Steel in 1930. During World War I, he served in the Chemical Warfare Service, U. S. Army, and in World War II he was closely associated with special metallurgical research for the armed services under the direction of the Office of Scientific Research and Development.

Among his special interests are stainless steels and welding, and in 1941 he was awarded the Lincoln Gold Medal of the AMERICAN WELDING SOCIETY.

Dr. Aborn is an active member of the AWS Welding Journal Committee.

Townsend Appointed Director

M. W. Townsend, assistant to the president, was appointed a director of Handy & Harman by the Board of Directors at their meeting on April 28th in New York.

M. W. Townsend

Mr. Townsend joined Handy & Harman, processors of silver, gold and platinum, in 1946; he served in the Sales Department and was sales manager prior to his present post.

Plan to Attend

1955 AWS NATIONAL FALL MEETING

October 17-21, 1955

Bellevue-Stratford Hotel

Philadelphia, Pa.

EMPLOYMENT SERVICE BULLETIN

Position Vacant

V-322. Welding engineer to head welding research section of a leading manufacturer of alloy, stainless, tool steels and high temperature alloys. A solid background of experience with various welding methods and techniques as applied to alloys difficult to weld is essential. Location, Pittsburgh. In reply, submit full resume, personal description and indicate salary requirement.

Services Available

A-669. Married. Age 38. Past six of 17 years on ferrous and nonferrous arc and resistance welding have been in planning, instruction or supervising. Recent experience with latest techniques in gas-shielded metal arc and inert arc on heat-resistant alloys under 0.125 in. thickness. Able to work with top management in all phases of setting up and operating a production or development shop. Good reason for desiring to relocate in Central States area.

A-670. Executive Engineer. 44 years of age. Heavy background in metal industry covering manufacturing, engineering and sales. Seasoned in all phases of welding, press work, metal finishing, inspection, time study, plant layout and modern manufacturing methods. Detailed in product design, development, tool engineering, production control, purchasing, marketing, styling and personnel relations. Experience covers shipbuilding, heavy equipment and high production methods of assembly in white appliance industry. Salary—five figures.

A-671. Welding Engineer. 38 years of age. Sc.D. in Metallurgy. 8 years' predoctoral experience in development and trouble-shooting, general metallurgy; 6 years' postdoctoral teaching, research and consulting. Chief interests now in applications and process development. Seeks position as welding metallurgist, or as materials and processes consultant to chief engineer. Patents and publications. Available August. All replies acknowledged.

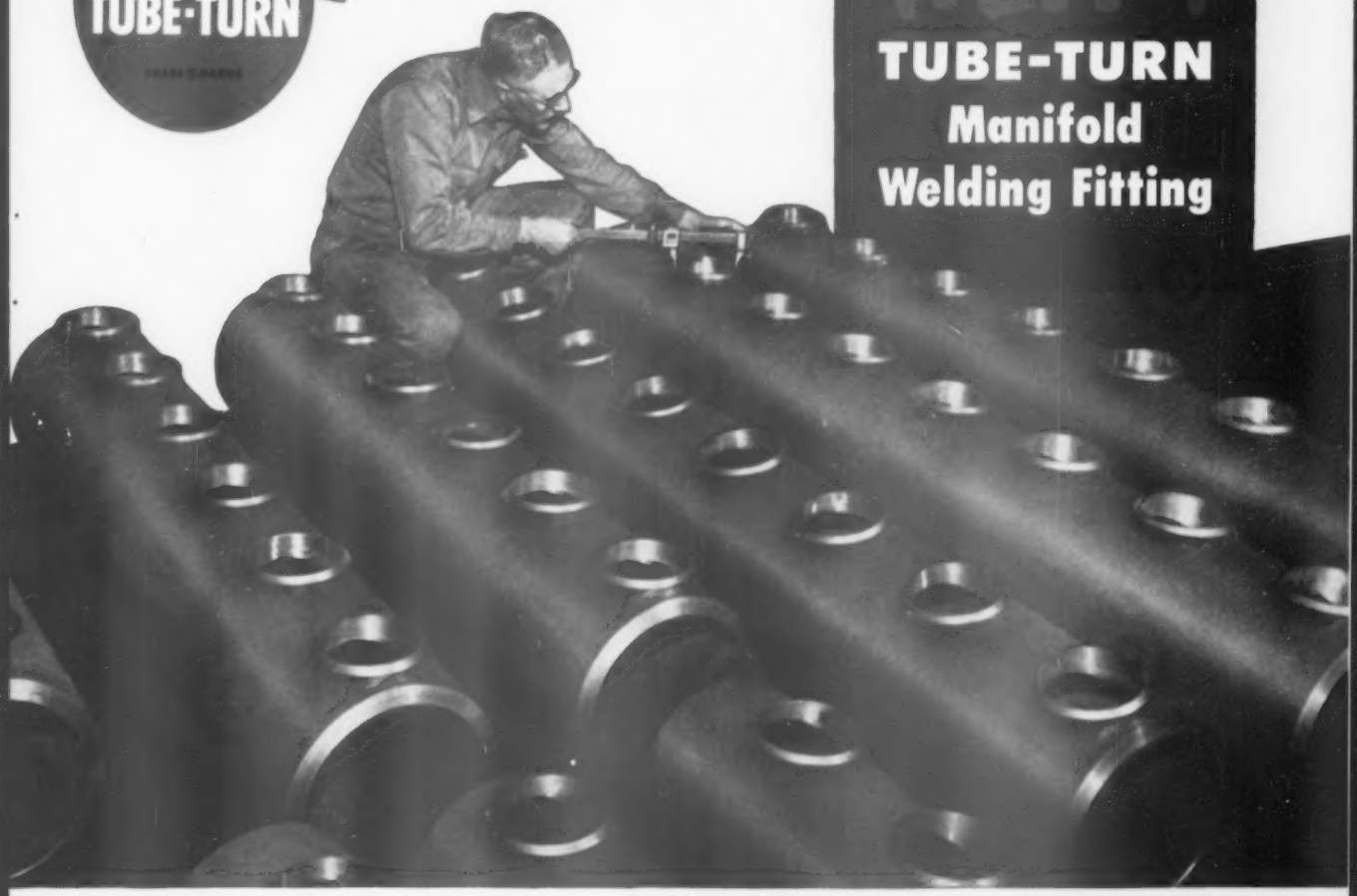
A-672. Welding Engineer. E.E. 17 years' first-class experience in the arc welding field, in design, application and production supervision. All processes, semi- and fully automatic setups, fixtures, studies in basic metallurgy. Age 40. Married. Position in production, welding engineering or research likewise welcome.

TUBE-TURN

Remember the trademarks "tt" and "TUBE-TURN" are applicable only to products of TUBE TURNS.

NEW!

TUBE-TURN Manifold Welding Fitting

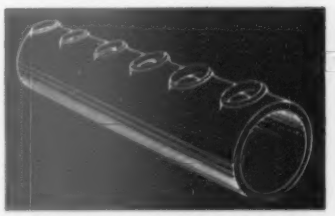


simplifies piping construction, reduces costs

TUBE TURNS now offers custom-made Manifold Welding Fittings with size, wall thickness, and number of outlets as required by your service conditions. These manifold fittings are available in all commercial metals and alloys.

Use of TUBE-TURN* Manifold Welding Fittings provides stronger and safer outlet connections than are possible when pads and saddles are employed. This new fitting makes possible greater economy and speed of construction in many types of service. It is particularly useful when outlets are smaller than one-half the run pipe diameter, when they are extremely close together, or when they must vary in size.

This new addition to Tube Turns' line enables you to "specify TUBE-TURN" for all your tee and manifold fitting requirements. For prompt, helpful service, get in touch with your Tube Turns' Distributor. You'll find one in every principal city.



The new TUBE-TURN Manifold Welding Fitting . . . superior to pad and saddle construction . . . easy to install . . . dependable in service.

*"TUBE-TURN" and "tt" Reg. U. S. Pat. Off.

TUBE-TURN Welding Fittings and Flanges are made in U. S. A.
They meet *all* U. S. piping code specifications

TUBE TURNS

LOUISVILLE 1, KENTUCKY

A Division of National Cylinder Gas Company

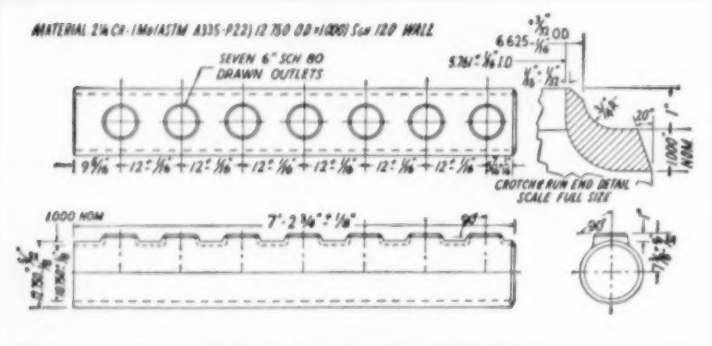
DISTRICT OFFICES: New York • Philadelphia • Pittsburgh • Cleveland • Detroit • Chicago • Beaver • Los Angeles San Francisco • Seattle • Atlanta • Tulsa • Houston • Dallas • Midland, Texas

APPLICATIONS

Why TUBE-TURN Manifold Welding Fittings give you safer service

In conventional pad and saddle construction, the transition to the side section of the branch is abrupt, creating stress concentration, often leading to failure. The TUBE-TURN* Manifold Welding Fitting with *integral extruded outlets,* is superior in strength. It provides a smooth transition from the pipe run to the smaller, thinner-walled outlets, and is thus better able to withstand cyclic stress from pressure or temperature changes.

TUBE-TURN Manifold Welding Fitting being installed in a measuring station of a large west coast gas utility.


MATERIAL 2¼ CR-1 Mo (ASTM A335-P22) 12.750 OD x (.1000) Sch 120 WALL
SEVEN 6" SCH 80 DRAWN OUTLETS
6.625-1/16
5.761-3/4 I.D.
20°
1000 MIN
CROTCH & RUN END DETAIL SCALE FULL SIZE
1000 NOM
7'-2 3/4" ± 1/8"
90°

12" Manifold with Seven 6" Outlets. Schedule 80.

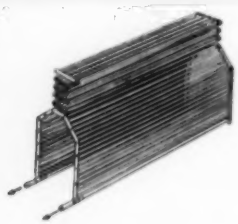
CODE APPROVED

TUBE-TURN Manifold Welding Fittings are in full accordance with the latest recommendations of ASA B31.1 Subcommittee No. 8.

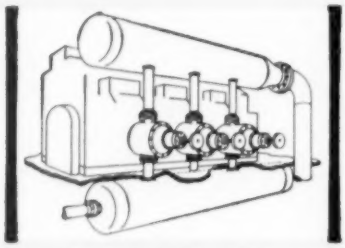
TUBE-TURN Manifold Welding Fittings comply with the requirements of ASA B16.9 and ASTM A234 covering the design and manufacture of butt welding fittings.

TYPICAL APPLICATIONS

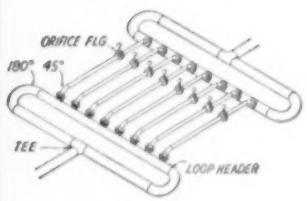
TUBE-TURN Manifold Welding Fittings can be used in virtually all types of major piping systems. Tube Turns' Sales Development Department will be glad to assist you in application problems.



Pipe Still or Charge Heater



Compressor Station Suction and Discharge Headers



Meter Run Loop Header

MANIFOLD DATA

Send for bulletin showing Manifold Welding Fitting Applications. Just mail the coupon.

TUBE TURNS, Dept. O-3
224 East Broadway, Louisville 1, Kentucky

Please send bulletin on new Manifold Welding Fittings.

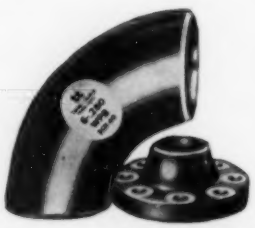
Company Name _____

Company Address _____

City _____ *Zone* ____ *State* _____

Your Name _____

Position _____



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New York	Los Angeles
Philadelphia	San Francisco
Pittsburgh	Seattle
Cleveland	Atlanta
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Denver	Dallas

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*"TUBE-TURN" and "⊞" Reg. U.S. Pat. Off.

TUBE TURNS

A DIVISION OF NATIONAL CYLINDER GAS COMPANY
LOUISVILLE 1, KENTUCKY

NEW PRODUCTS

Surface Flow Control

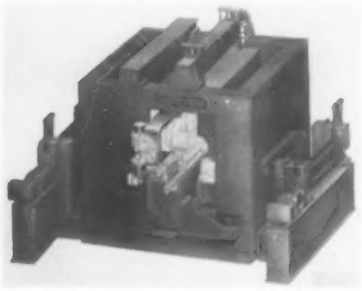
A new liquid which will control the surface flow of any known brazing alloy is now available from Stainless Processing Division of Wall Colmonoy Corp., 19345 John R. St., Detroit 3, Mich. The new material can be used effectively with all nonferrous metals, including titanium and with most alloy steels, including stainless.

Called Nicrobraz Green Stop-Off, the new liquid is claimed to permit hairline control in preventing the flow of copper, nickel and silver brazing alloys across the surface of any type of brazed assembly and is applicable to any brazing method.

For additional information, write directly to manufacturer at above address.

Shear-Weld-Trimmer

The Morton Model RB Shear-Weld-Trimmer shown in the accompanying photograph has been specifically designed for joining the ends of coils in continuous process lines. Galvanizing,



pickling, annealing and slitting lines are a few of the many continuous processes requiring equipment of this nature.

For full information, write to Morton Manufacturing Co., Broadway and Hoyt, Muskegon Heights, Mich.

Hard-Facing Alloys

A new line of tubular fabricated alloy wires designed for open arc application through standard semiautomatic welders has just been announced by Stoody Co. Reportedly, this is a new process that requires no flux and offers the versatility of manual welding with the control of the full automatic.

Three materials are now in production at the Stoody Co. plant in Whittier, Calif.—a 50% high-chromium hard-facing alloy, a 20% alloy for hard facing and a Hadfield nickel manganese for build up of manganese steel parts. In operation, semiautomatic welding using the Stoody tubular wires is much like manual welding but wire feed is continuous and both feed and arc control are automatic.

For complete information, write to Stoody Co., Whittier, Calif.

A-C Transformer Welders

Operating convenience has been stressed in design of a new line of a-c transformer-type arc welders announced by Hobart Brothers Co., Troy, Ohio.

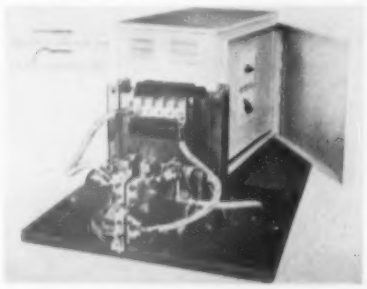
Model TLP-253-S (with capacitors) and TL-253-S (without capacitors) are dual rated at 250 amp on 30% duty cycle, or 200 amp on 50% duty cycle for operation on single-phase, 60-cycle, 230-v supply line.

Welding current selection is made through a simple twist of a rubber hand wheel and rheostat knob. Both voltage and amperage are controlled to give the desired control of the arc characteristics for a variety of welding jobs.

A sturdy lifting eye is provided for easy handling. Also available with three wheel portable mounting at nominal extra cost. Write Hobart Bros. Co., Troy, Ohio, for further information.

Butt Welder

A semiautomatic oil, hydraulically operated butt-welding machine for butt welding materials 0.015 in. diam up to $^1/_8$ in. diam and tubing $^1/_{16}$ in. OD up to $^1/_4$ in. OD has recently been designed and manufactured by Federal Tool En-



gineering Co. (Tweezer-Weld) of Cedar Grove, N. J. Welding can be accomplished by means of a condenser discharge power supply or by a synchronous timer operating from 1 kva up to 5 kva. For further information write directly to manufacturer.

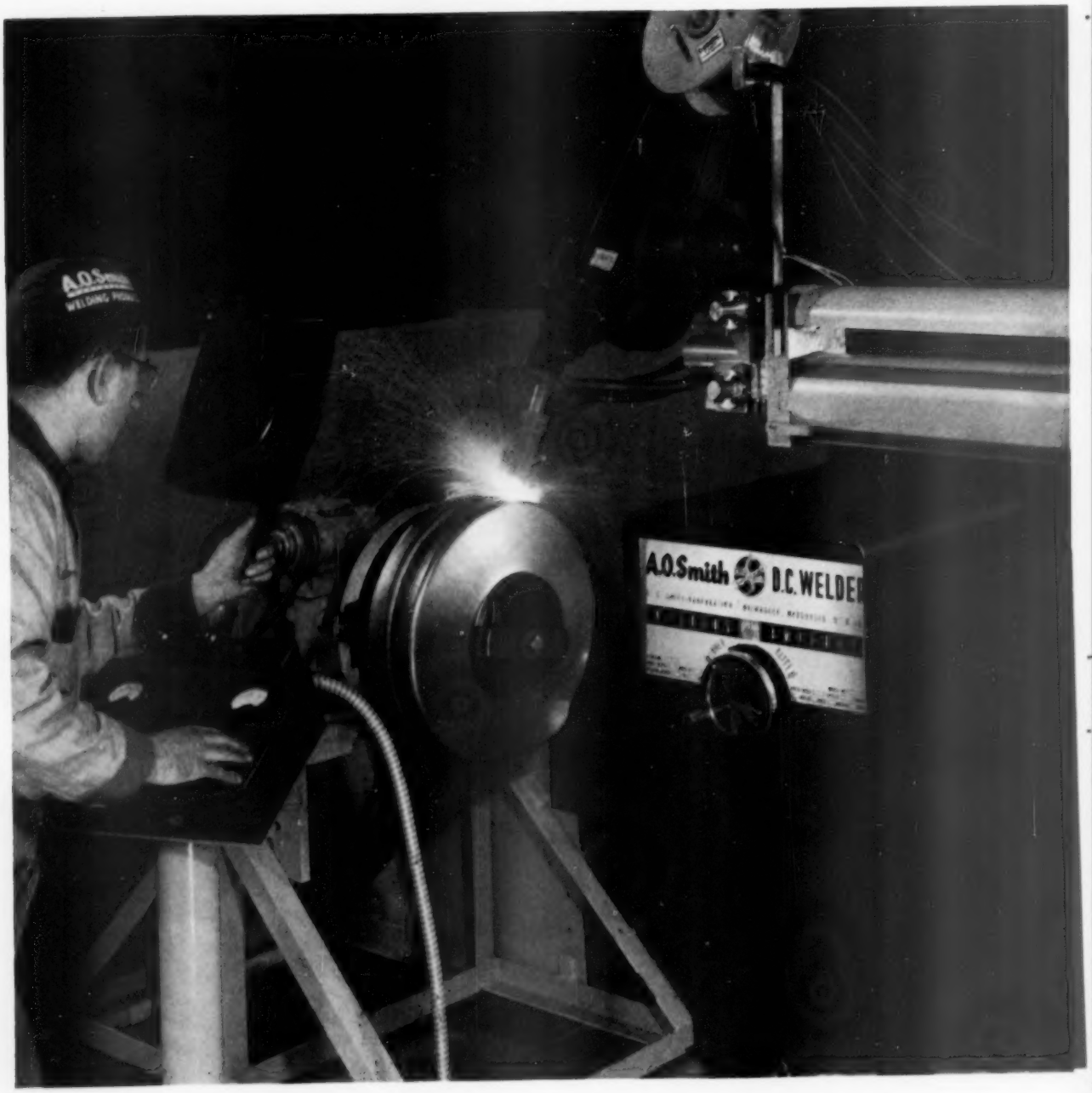
Portable Band Saw

An electric metal-cutting band saw that is not just movable but completely portable has been introduced by the Porter-Cable Machine Co., Syracuse, N. Y. The new Model 524 Porta-Band Saw weighs only 16 lb, is reported to be scarcely larger than a hand hack saw and 15 times faster by actual test.

Position _____

C-O-MATIC

NOW! Weld even mild and



announced by A. O. Smith

medium carbon steels automatically...

new gas-shielded metal arc process uses Carbon Dioxide to shield the arc

ON GAS ALONE **in one year's time (3 shifts per day, 260 days), a single C-OMATIC unit saves you *$14,902.00 over Argon, $15,367.00 over Helium.***

● **NEW ECONOMY** thanks to use of less-costly Carbon Dioxide . . . thanks to low maintenance requirements of functional design.

● **X-RAY QUALITY WELDS** — Strength and ductility — yours with C-OMATIC.

● **VISIBLE ARC** means faster, easier welding . . . quick, convenient spotting of nozzle. (When required, C-OMATIC is easily convertible to submerged arc, too.)

● **DEEP PENETRATION** further assures high quality welds. No slag to remove . . . no flux to clean up.

● **ACCURATE CONTROL SYSTEM** of constant current design maintains preset arc voltage without fluctuation. No complex electronic circuits! Auxiliary controls available.

● **POWER SOURCE** — The service-proved A. O. Smith d-c rectifier.

Chart illustrates astonishing savings

	HELIUM	C-OMATIC			ARGON	C-OMATIC	
Gas consumption per hour	70 cu. ft.	30 cu. ft.		Gas consumption per hour	60 cu. ft.	30 cu. ft.	
Cost of gas per cu. ft.	$0.067	$0.01		Cost of gas per cu. ft.	$0.09	$0.01	
Cost of gas per hour	$4.69	$0.30		Cost of gas per hour	$5.40	$0.30	
Cost per hour 60% of duty cycle	$2.81	$0.18		Cost per hour 50% of duty cycle	$2.70	$0.15	
Cost per day (3 shifts, 22.5 hours)	$63.20	$4.05		Cost per day (3 shifts, 22.5 hours)	$60.80	$3.38	
Cost per year (260 days)	$16,420	$1,053	$15,367	Cost per year (260 days)	$15,780	$878	$14,902

GET ALL THE FACTS NOW! Write for free booklet describing C-OMATIC in detail. Welding Products Division, A. O. Smith Corporation, Milwaukee 1, Wisconsin.

See C-OMATIC in action at the AWS show in Kansas City, Mo. . . . June 8 to 10 . . . Booth 256.

Through research . . . *a better way*

A.O.Smith

CORPORATION

WELDING PRODUCTS DIVISION

Milwaukee 1, Wisconsin

INTERNATIONAL DIVISION: MILWAUKEE 1, WIS.

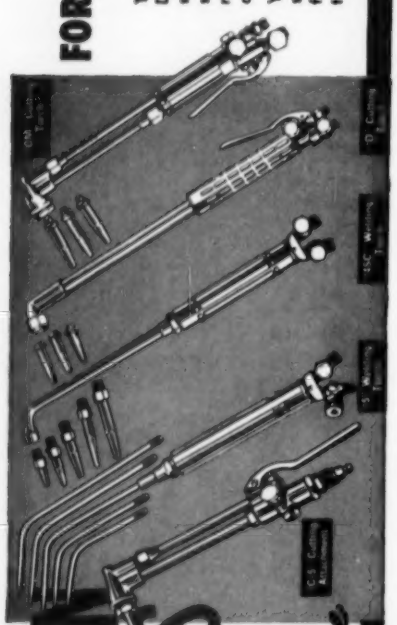
FOR OXY-ACETYLENE WELDING AND CUTTING

There is a Dockson Better-Built Torch for all your jobs. Dockson Torches incorporate a new, longer life, better seal compound developed especially for oxy-acetylene service in both the mixer and union rings. Dockson's two-ring construction means fewer rings to replace, stronger construction, prevention of possible breakthrough.

Your DOCKSON DISTRIBUTOR—a selected specialist, stocks a complete line of Dockson products to give you fast service and personal attention. We back our distributors 100%.

WRITE FOR YOUR DISTRIBUTOR'S NAME AND COMPLETE CATALOG OF WELDING AND CUTTING EQUIPMENT.

Dockson CORPORATION
3339 WABASH • DETROIT 8, MICH.



DOCKSON TORCHES
Built for Better Service

It is claimed to be the only metal-cutting saw ever made that is fast and light enough to be used free-hand in any position. The 524 with a stock-cutting capacity up to $3^1/_4$ by $4^1/_4$ in. is equal to a large hack saw. Equipped with a powerful motor, it can cut ferrous and nonferrous metals, plastics and other problem materials with greater speed and accuracy, according to the manufacturer.

For complete information and price of Porta-Band Saw and 44-page illustrated catalog on other tools and supplies, write Porter-Cable Machine Co., 33 Exchange St., Syracuse 8, N. Y.

Toggle Clamps

Erico Products, Inc., 2070 E. 61st Place, Cleveland 3, Ohio, announces the introduction of the "Caddy" Type H vertical pressure toggle clamp.

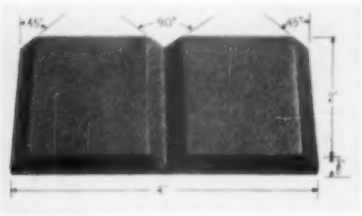
Available in three sizes, they have the following clamping pressures: Model H-O, 800 lb, Model H-1, 1000 lb and Model H-2, 2000 lb.

The clamp is designed for use on jobs with limited space for vertical handle such as found on "Caddy" Type V clamps.

For further information, write directly to the manufacturer.

Test Plate

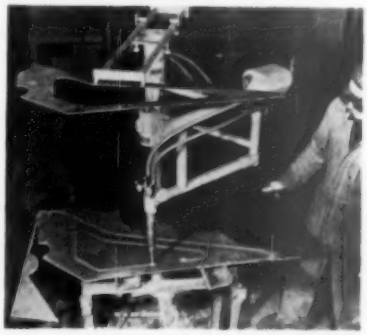
The Weld Rod Division of the Chicago Hardware Foundry Co. is introducing a new test plate called "Test-Well." The manufacturer claims that the new plate will furnish perfect tests at a fraction of the cost of cutting, grooving, etc.



Tests may be made for penetration, color match, weld inclusions, by putting a fillet in the V or by breaking the casting in half—which is easily done with a hammer—and welding the two halves together.

For further information write directly to Chicago Hardware Foundry Co., North Chicago, Ill.

Contour Cutting Machine



One of the Ultra-Graph oxygen cutting machines, developed by the Heath Engineering Company of Fort Collins, Colo., is shown in operation at the Bos-Hatten, Inc., plant in Buffalo, N. Y. Note that the tracing head is in the same center-line as the torch—in fact, precisely in the same center-line as the cutting orifice in the torch. According to manufacturer, no distortion is experienced in cutting.

Resistance Welding Guns

An entirely new concept in portable resistance welding gun units is claimed to have been developed by the Craft Welding Equipment Co., Detroit.

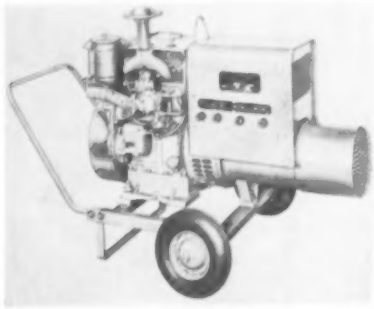
One of the features of Craft portable welding guns is reported to be the complete interchangeability of parts between the various gun types in this line.

According to Craft welding engineers, these guns incorporate an extra long bushing, exceeding length of stroke, which eliminates bearing failure due to flash carry-back. Additional features emphasized are the interchangeable jaw extensions on the Craft units, reducing to a minimum the number of gun types necessary to any welding operation.

A six-page illustrated folder incorporating specifications and construction details of these products can be obtained by writing to Craft Welding Equipment Co., 738 W. Seven Mile Road, Detroit 3, Mich.

Engine-Driven Welder

Light weight, small space requirements, and low cost in a heavy-duty unit are some of the important advantages claimed by the Welding Division of the Harnischfeger Corp. for its new WN-180 engine-driven arc welder. Because this is a light-weight, heavy-duty machine that welds, cuts, solders, brazes or hard surfaces, it is said to be ideal for heavy construction work as well as for garage, farm and repair shop application.



Weighing only 425 lb, the WN-180 has a service range of 20 to 200 amp. Its outside dimensions are 45 x 29 x 21 in. The unit is powered with an industrial-type 12-hp, 2-cylinder, 2000 rpm Wisconsin air-cooled engine.

Further information about the new WN-180 engine-driven arc welder is available from the Welding Division, Harnischfeger Corp., Milwaukee 46, Wis., or any of its district offices or welding distributors.

Brazing Equipment

Fast, dependable automatic brazing for many types of small and medium-size assemblies is provided by their new self-contained, gas-fired package unit, according to the Selas Corporation of America, Philadelphia 34, Pa.

The packaged unit reportedly uses precise control of combustion and automatic timing to produce sound brazed joints of uniform high quality at high speed.

Complete details may be had by writing directly to Selas Corp. at the company address.

Pressure Gages

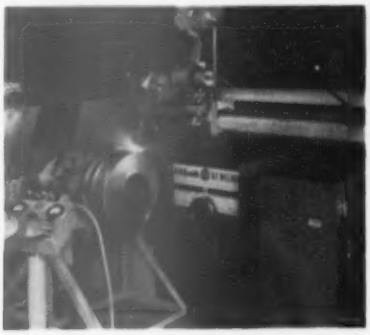
Dockson Corp., 3829 Wabash Ave., Detroit 8, Mich., has announced the development of the new No. 55 "Economizer" gage for use with oxygen, acetylene, air and CO_2.

The body and bonnet of this new gage are forged, solid brass. Standard Dockson construction principles developed through years of field testing are incorporated in the new "Economizer."

Complete information of the new No. 55 Economizer gages and name of your local Dockson distributor is available from the Detroit address.

"C-omatic" Process

A new, automatic welding process known as "C-omatic," which uses low-cost carbon dioxide gas for shielding the arc while welding mild and medium carbon steel, has been developed by the A. O. Smith Corp. of Milwaukee. It is similar in operation to the other



existing consumable-electrode inert-gas metal-arc processes.

According to the manufacturer, the "C-omatic" process offers the advan-

AT THE FRONTIERS OF PROGRESS YOU'LL FIND

AN AIR REDUCTION PRODUCT

FOR WELDING AND CUTTING...

IN THE RED DRUM

HIGHEST QUALITY

DUST-FREE

NATIONAL CARBIDE

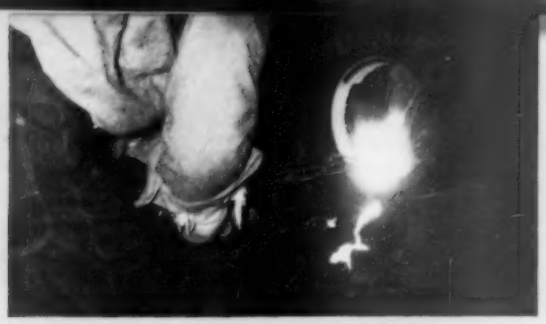
Write for the name and address of the NATIONAL CARBIDE supplier nearest you.

National Carbide Company

GENERAL OFFICES: 60 EAST 42ND STREET, NEW YORK 17, N. Y.

A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED

NCG Torchweld® torches for Ferrojet cutting are available in models for either machine or hand cutting. Illustration shows the Ferrojet machine cutting torch adapted to the NCG Cut-O-Matic® portable cutting machine.



Riser pads on this stainless steel casting are being removed in half the time with the Ferrojet flame washing torch. It may also be used for removing cracks, chill bars, fins and sand inclusions from stainless and high alloy castings.

NCG FERROJET® Dispenser teams up with CUT POWDER-CUTTING

Mounted on a standard, two-wheel gas cylinder truck, the dispenser plus a cylinder of either compressed air or Nyrogen® make a mobile, easy-to-handle outfit. Dispenser does not require electricity . . . can be operated anywhere.

NCG Ferrojet Dispenser costs up to 70% less than expensive makes . . . gives unsurpassed performance

If it were possible for you to compare all the various powder dispensers, right in your own shop, on your own work, you would probably choose the NCG Ferrojet Dispenser. Here's why:

You save up to $600 on each dispenser you buy, because NCG's simplified dispensing principle provides a uniform flow of powder without using a complicated powder feeding mechanism. But low price is only half of the story.

Even more important than initial savings is its outstanding performance. For no other dispenser, regardless of cost, is capable of delivering more precise or uniform flow of powder. (And uniform flow, of course, is the basis of successful powder cutting.) No other dispenser is as easy to operate and maintain.

CHOICE OF TWO MODELS

In the automatic Model 301 Ferrojet Dispenser, gas pressures and amount of powder flow are maintained automatically. Special powder control operates simultaneously with turning-on and shutting-off of cutting oxygen valve on torch. Powder flow must be turned off manually on semi-automatic Model 302 Ferrojet Dispenser.

Ferrojet equipment is readily adaptable to machine cutting operations. Here a large diameter is being Ferrojet-cut from a 5½" thick plate by one of the country's large producers of cut-to-order stainless steel shapes.

new type powder to COSTS...

The Ferrojet hand cutting torch, shown cutting risers from a heavy, stainless steel casting, is widely used in foundries, fabricating plants and in scrapping operations. Tips are available for Ferrojet-cutting up to 16" thicknesses in steel.

Amazing new NCG Ferroflame® Powder speeds cutting and washing, reduces powder consumption

Unlike ordinary ferrous powders that are made up of solid, relatively thick particles, the revolutionary new NCG Ferroflame powder consists of tiny spheres crushed into extremely thin flakes.

The greater surface area of these flakes, as contrasted to solid particles, makes Ferroflame powder burn much more rapidly and with a higher concentration of heat. This means faster, easier cutting and reduced powder consumption.

NCG Ferroflame powder is available in two types: Type A for general purpose cutting, and Type B for washing operations. Both types packed in air-tight 100-lb. containers.

NCG®

EVERYTHING FOR WELDING

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NATIONAL CYLINDER GAS COMPANY • 840 N. Michigan Ave., Chicago 11, Illinois

Branches and Dealers from Coast to Coast

EVERYTHING FOR POWDER-CUTTING FROM ONE SOURCE!

You get all four with NCG—equipment, gases, powder and skilled technical assistance. Find out more about this complete powder-cutting service. Phone or write your nearest NCG representative or dealer, or write for information.

tage of low-cost automatic welding plus the benefit of a visible arc, which enables the welder to properly locate the arc in the welding groove and observe the weld metal being deposited.

For complete information, write to A. O. Smith Corp., Welding Products Division, Milwaukee 1, Wis.

Safety Helmets

Workers on jobs that involve head-injury hazards are offered added protection in the new Skullgard plastic

safety helmet introduced by Mine Safety Appliances Co., Pittsburgh, Pa.

Designed to offer extra protection in what has been described as the "danger zone"—the front, top and back areas of a hat that working positions expose most often to impact—the new Skullgard incorporates a special reinforcement in this zone's three vital areas.

For full details, write directly to the company.

Welding Helmet

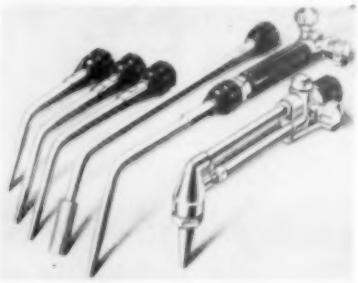
A new-type welding helmet "Chin-Lense" that frees both hands for welding at all times by permitting the operator to raise or lower the welding lens with a slight pressure of the chin is now available from the New Products Division of Carruthers and Fernandez, Inc., Santa Monica, Calif. It features a patented chin pad that requires only a

1/4-in. motion of the chin to drop the welding lens, giving complete visibility through the clear inside safety glass and filter lens which protect the welder's eyes. Removal of the slight pressure instantly returns the welding lens to its perfectly sealed welding position.

For complete details, write to Carruthers and Fernandez, Inc., New Products Division, 1501 Colorado, Santa Monica, Calif.

Marquette Torches

Four all new oxy-acetylene torches known as the heavy-duty Super-Jet, the medium duty Star-Jet, the light duty Aero-Jet and the automatic light duty Jet-o-Matic are announced by the Marquette Manufacturing Co., Inc.

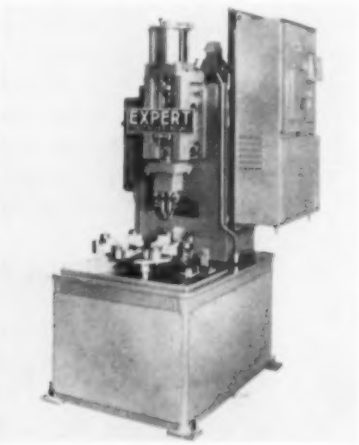


The outstanding features claimed for these torches are greater ease of operation and greater reliability.

For further information write the manufacturer, Marquette Manufacturing Co., Inc., 307 E. Hennepin Ave., Dept. NS, Minneapolis, Minn.

Index Machine

A new eight-station index machine for a wide variety of spot, projection and arc welding production operations is now available from Expert Welding Machine Co., 17144 Mt. Elliott Ave.,



Be Wise . . . Economize

REBUILD WORN GYRATORY CRUSHERS with

MANGANAL

T. M. Reg. U. S. Pat. Off.

11% - 13½% MANGANESE - NICKEL STEEL ROUND APPLICATOR BARS

- Costs less and gives longer service than new crushers.
- Brings efficiency of crusher back to peak performance fast.
- Minimizes down-time; cuts lost production hours.
- Quickly and easily attached with Manganal Welding Electrodes.

WRITE for literature on additional money-saving applications.

STULZ-SICKLES CO.

SOLE PRODUCERS 92 N. J. RAILROAD AVE. NEWARK, N. J.

NEAREST DISTRIBUTOR UPON REQUEST

Detroit 12, Mich. This compact machine, which will produce up to 720 welded assemblies per hour, is a simple, compact design that features an all-mechanical drive.

For complete details, write to the company at the above address.

Grinding Wheels

The Blue Flash "BF" raised hub disk-wheel, Bay State's newest portable grinding wheel development, supposedly delivers substantial increases in cutting speed.

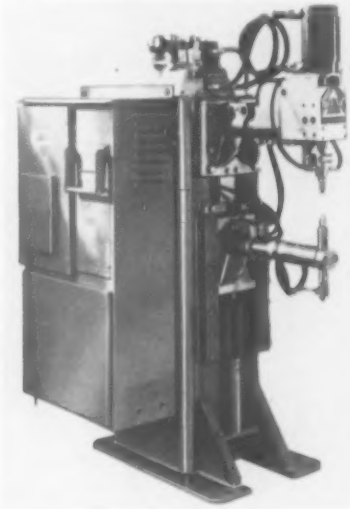
Strongly reinforced internally and on the back, the wheel has been designed for all weld grinding and portable cutoff applications found in metal fabricating plants and foundries.

Write for further details to Bay State Abrasive Products Co., Westboro, Mass.

Resistance Welder

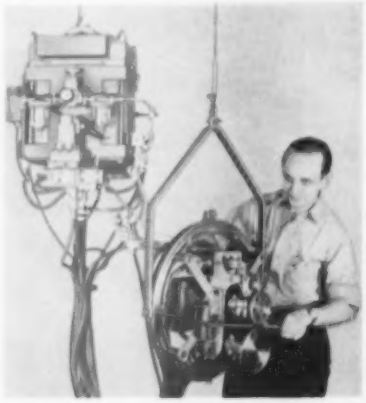
Recently introduced by the Delta Welder Corp., Detroit, was a new high production, heavy-duty spot welder. It is stated that this machine is designed for high-duty, cycle resistance welding on medium gage sheet metals.

For full details on the Delta spot welder, write to Delta Welder Corp., 8525 Livernois, Detroit 4, Mich.



Portable Seam Welder

Progressive Welder Sales Co., 3070 E. Outer Drive, Detroit 34, Mich., announce a new portable seam welder designed to weld 200 ipm.



Originally developed to weld aircraft drop tanks during the Second World War, the Progressive Portable Seam Welder is lighter yet sturdier than previous models, thus reducing operator fatigue

Complete information is available from Progressive on the new unit.

AVAILABLE NOW...
new, improved Westinghouse MC-180 farm and shop welder

Ideal for heavy farm and shop construction or maintenance. Continuous current adjustment; no plugs, taps, switches to change. Rugged, durable, dependable, MC-4G-180 welds with electrodes up to 5/16 inch. Operates on 230-volt, a-c current.

Complete with all accessories plus easy-to-follow welding instructions. Order yours today. Contact your local Westinghouse distributor. Or write, Westinghouse Electric Corp., Welding Dept., Box 868, Pittsburgh 30, Pa. J-21892

YOU CAN BE SURE...IF IT'S Westinghouse

Get the BEST for LESS
Get
"ANTI-BORAX" FLUXES
Fully Guaranteed
SAFE—NON-TOXIC

No. 1 Cast Iron Welding Flux
No. 2 Brazing Flux
No. 4 "Braz-Cast" Flux for bronze welding cast iron
No. 8 "ABC" Sheet Aluminum Flux
No. 16 Silver Solder Paste Flux.

Send for complete Folder

Mfg. by
ANTI-BORAX COMPOUND CO. INC.
Fort Wayne 9, Indiana

Advertising Rates

The Welding Journal

Effective June 1, 1955	1 time	3 times	6 times	12 times
Full Page	$305.00	$280.00	$265.00	$240.00
Two-thirds Page	215.00	200.00	185.00	175.00
Half Page	170.00	155.00	145.00	130.00
One-third Page	125.00	115.00	110.00	105.00
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VICKERS® Controlarc

MAGNETIC AMPLIFIER-RECTIFIER

FULL TIME,

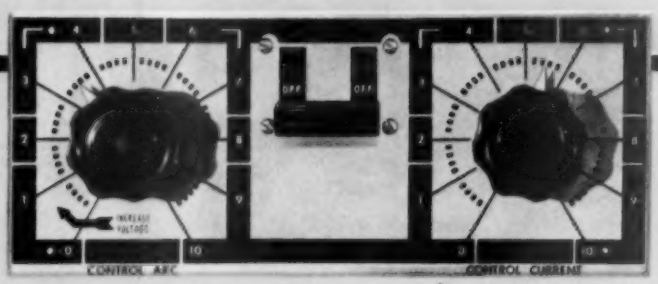
INDIVIDUAL CONTROL
of both current and voltage

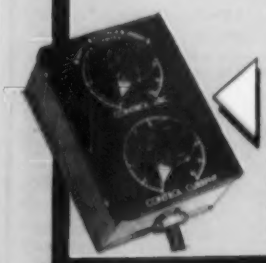
- EASY, CLEAN ARC STRIKES
- HOT STARTS, IMMEDIATE PENETRATION
- NO ARC "POP OUTS"
- NO CURRENT DROP-OFF DURING PASS
- UNIFORM FUSION, STEADY RATE OF METAL DEPOSIT

PATENTED VICKERS MAGNETIC AMPLIFIERS

CONTROLARC automatic control is made possible by the patented Vickers Self-saturating Magnetic Amplifier with exclusive voltage-sensing, feed-back control circuits. Constantly analyzing welding voltage and automatically adjusting welding current to arc changes, Vickers Magnetic Amplifier maintains ideal arc characteristics, through the entire welding range, under all welding conditions.

VOLTAGE and CURRENT INDIVIDUALLY CONTROLLED...





Only Vickers Controlarc offers complete remote control of both voltage and current through the entire welding range. The operator never has to go to the welder to change range switches or taps. Two separate dials individually control voltage and current.

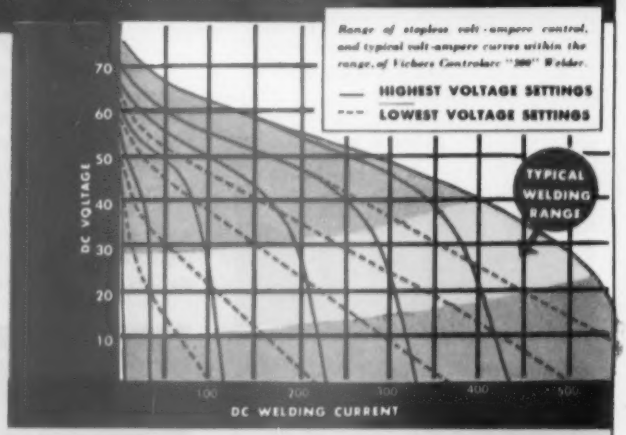
Separate, complete control of both voltage and current makes it possible to set any volt-ampere combination within the output range of the particular Controlarc model being used —from minimum voltage at minimum current to maximum voltage at maximum current.

DC Welder 200 300 400 AMPERE MODELS

AUTOMATIC ARC CONTROL

FULL RANGE VOLTAGE CONTROL

. . . separate from current control, makes it possible to choose the exact arc characteristic for every job and every welding condition. Controlarc full range, stepless voltage control is not limited to a few "average" settings, with blind spots in between. With Controlarc voltage control, the weldor not only can select the arc characteristic for a basic welding position—flat, overhead or vertical—but can pinpoint the setting to his personal preference for the particular condition under which he is welding.



ELECTRICAL SPECIFICATIONS and RATINGS

The Vickers CONTROLARC DC Arc Welder is rated in accordance with NEMA Standards for Rectifier type arc welders.

RATINGS					ELECTRICAL PERFORMANCE				
Rated Amperes at 40 Volts Load, 60% Duty Cycle	Amperes Welding Current Range		Primary Amperes Input per Phase		RATED AMPERES	AT RATED LOAD			
	Minimum at 20 Load Volts	Maximum at 40 Load Volts	230 Volts	460 Volts		PRIMARY KVA	PRIMARY KW	EFFICIENCY %	POWER FACTOR %
200	3	250	42	21	200	16.5	11.6	69	71
300	6	375	62	31	300	24.7	17.4	69	71
400	12	500	84	42	400	33	23.2	69	71

VICKERS CONTROLARC DC WELDER
Construction Features

DOWNDRAFT COOLING
No "vacuum cleaner" floor dust pickup

RUST PROTECTED
Interior surfaces zinc chromated

RECTIFIERS
Conservatively rated for long life. Special finish protects from corrosive atmospheres

SEALED-IN LUBRICATION OF FAN MOTOR BEARINGS

DUCT COOLED TRANSFORMER
Air ducts in windings for high-efficiency cooling

CONCENTRATED WEIGHT
Low center of gravity

SKID MOUNTED
Portable mounting available

NO MOVING CORES OR COIL ASSEMBLIES
NO GEAR OR OTHER MECHANICAL DRIVES
ALL PARTS QUICKLY AND EASILY ACCESSIBLE

TERRITORIES OPEN FOR QUALIFIED DEALERS. WRITE FOR DETAILS.

VICKERS ELECTRIC DIVISION

VICKERS Inc. a unit of The Sperry Corporation

1815 LOCUST STREET • SAINT LOUIS 3, MISSOURI

Printed in U. S. A.

Radiographic Equipment

To simplify radiographic testing methods, the Balteau Electric Corp., Stamford, Conn., has developed a new

hydraulic crane for handling the "camera" of its 200-kv industrial X-ray unit.

According to the manufacturer, the crane is designed to serve either as a stationary mounting or as a mobile base that can be used for on-location inspection throughout the factory; it permits the operator to raise and lower the "camera" or X-ray head to a maximum height of 87 in. and a minimum height of 24 in. An extension boom is available that widens the range of movement to a low of 14 in. and a high of 101 in. The head may be readily rotated through 360 deg around either vertical or horizontal axes.

•

Mechanical Presses for Resistance Welding

A new line of compact low-tonnage inverted mechanical presses designed for a wide variety of spot- and projection-resistance welding operations is now available from Expert Welding Machine Division, 17144 Mt. Elliott Ave., Detroit 12, Mich. The machines are made in a line of standard 32-, 42- and 60-in. models, each having adjustable strokes of 5, 6 and 8 in.

These Expert mechanical presses are rated at about 2-ton capacity. They have large upper and power platen areas that extend their application into other production operations. Maximum operating speed of the presses is 2200 strokes per hour.

Additional information may be had by writing to the manufacturer at the above address.

•

New Inco Electrodes

The International Nickel Co. has announced the development of two new welding electrode: (1) Inco-Rod "A," a multipurpose rod, which is expected to reduce substantially electrode inventories in the average plant; and (2) "132" Inconel electrode, which welds Inconel to itself.

Inco-Rod "A," designed to fill a long existing need for an electrode capable of making sound joints between a large number of metals and alloys of different compositions, is expected to supplant

many of the electrodes now required in plant stocks. It is anticipated by Inco that this electrode will give high-quality welds on better than 90% of the dissimilar welding jobs. The area of its greatest usefulness will be in the joining of ferritic (mild and low-alloy steels) materials, to stainless steels. In addition, the new electrode permits these groups of alloys to be successfully joined to high nickel alloys, such as Monel and Inconel.

The principal advantage of the improved "132" Inconel electrode is that radiographic quality welds are possible with the electrode in the as-received condition. Previously it was necessary for the user to rebake the electrode in his plant to insure top-quality welds.

CHECK THESE EXCLUSIVE ADVANTAGES *of the* NEW

HI-AMP HOLDER

-and You'll Wonder Why You're Not Using them Now!

- Spring completely enclosed and insulated. Cannot be knocked or shorted out.
- Spring adjustment screw for ease of replacement and adjustment to rod size.
- Handle cannot absorb moisture-withstands higher temperatures.
- Tip Insulator reversible for longer service life.
- Low Trigger for ease of rod-relief-cuts down operator fatigue.
- Slender rod gripping tongues to get in tight places.

Sold only through Welding Supply Distributors in the U. S. and Canada.

WAGNER MFG. CO.
350 W. 1st SOUTH ST.
JACKSON, MISSOURI

Weld with Wagner

RESISTANCE WELDERS

Spot & Projection Welders

15 KVA NATIONAL, 440 V, 4" throat, air operated
15 KVA NATIONAL 440 V, 7½" throat, air operated
20 KVA T-W 440 V, 6" throat, air operated
20 KVA TAYLOR-WINFIELD 220 V, 12" throat
30 KVA T-W 440 V, 24" rotary table
125 KVA NATIONAL 440 V, 36" Dial Feed
175 KVA NATIONAL 440 V, 12" throat, air operated
300 KVA NATIONAL, 440 V, 12" throat, hydraulic
350 KVA SWIFT, 440 V, 12" throat, air operated
350 KVA HI-SPEED, 440 V, 36" throat, hydraulic
50 KVA RESISTANCE, 5 trans., 20 guns

Seam Welders

100 KVA SCIAKY 440 V, 30" throat, air operated, longitudinal type.
200 KVA PROGRESSIVE 440 V, 2 " throat, air operated, longitudinal type.
200 KVA FEDERAL Box Welder, 440 V, 24" throat, hydraulic table long. travel 25" cross travel 9".
200 KVA FEDERAL 440 V, 36" throat, air operated, longitudinal type.
200 KVA FEDERAL 440 V, 48" throat, air operated, longitudinal type.

Flash Welders

50 KVA THOMPSON-GIBBS 440 V, Air upset.
100 KVA NATIONAL 440 V, hydraulic upset, hydraulic operated alligator clamp.
150 KVA T-W 220 V, hand clamp, hydraulic upset
300 KVA NATIONAL Barrel Welder, 30" die width, hydraulic operation.

Certified Aluminum Welders, 3 Phase

50 KVA TAYLOR-WINFIELD, 440 V, 36" throat, rocker arm, air operated
100 KVA SCIAKY PMCO 2ST-100-36 440 V, 36" throat, air operated.
150 KVA SCIAKY PMM2TC, 36" throat 440 V, universal seam welder.

WEBER MACHINERY CO.

Diamond 1-8311

8200 Bessemer Ave. • Cleveland 27, Ohio

NEW LITERATURE

ASTM Standards

The American Society for Testing Materials announces that the 1954 Supplements to the 1952 Book of ASTM Standards which is issued triennially are now available. Together with the 1952 Book of ASTM Standards and the 1953 Supplements which are published about a year ago, they give in their latest form all ASTM specifications, tests, definitions, etc., except the chemical analysis of metals.

RWMA Report

First quarter resistance welding machine shipments have increased 16% over the same quarter of 1954, according to an announcement of the Resistance Welder Manufacturers' Assn., at a meeting held in Detroit, Mich., April 22nd.

One of the current pro ects of RWMA is the issuance of a brochure depicting the educational, research and technical activities of RWMA which have been instituted to assist metal fabricators.

Over 100,000 copies of the RWMA Technical-Educational Bulletins have been printed and distributed to resistance welding users and to engineering schools in the past few years, according to a report presented to members recently.

In addition to the RWMA series of bulletins, the Association also makes available to plants and schools, two films—"The Resistance Welding of Stainless Steel" and "This Is Resistance Welding."

Welding of Stainless Steels

The 1955 edition of the handbook *The Welding of Stainless Steels*, published by the Electrode Division, McKay Co., is now ready for distribution.

Presented for the most part in easy-to-follow question and answer form, the handbook describes the various types and properties of stainless steels, the practical procedures for their welding, and the types of electrodes available for such use.

This informative 48-page handbook is available, upon request, from the McKay Co., 303 McKay Building, Pittsburgh 22, Pa.

Ampco Welding News

Ampco Metal, Inc., has released the first quarter issue of its publication, *Ampco Welding News*.

The feature article in this issue describes the overlay of Jackson and Church Co. cast-iron pulp press spindle flights with Ampco-Trode 10 and Ampco-Trode 200 electrode deposits to resist wear and corrosion. Other interesting articles cover "Selection of Resistance Welding Electrodes," the inlay of special high torque power transmission gears, the fabrication of minesweeper rudders, the overlay of toggle levers and the repair of manganese bronze castings with Mang-Trode.

Free copies of this issue can be obtained by writing Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Joining of Stainless Steels

Crucible Steel Company of America announces the availability of a newly revised 24-page booklet entitled "The Joining of Crucible Rezistal Stainless Steels." The booklet describes in detail 17 methods employed in joining stainless steel by fusion processes.

These processes include manual, automatic and semiautomatic welding, brazing and soldering.

The booklet also contains a section describing eight flame and arc cutting procedures commonly used for severing stainless steel.

For a copy of this publication write the Advertising Department, Crucible Steel Company of America, Box 88, Pittsburgh 30, Pa.

Stainless Steel Brazing

A new $8^1/_2$ x 11, two-color, four-page catalog describing the properties and applications of Nicrobraz stainless steel brazing alloy is now available from Stainless Processing Division, Wall Colmonoy Corp., 19345 John R. St., Detroit 3, Mich.

The new catalog describes Nicrobraz and discusses its physical and chemical properties. Included are typical curves which chart shear strength, ductility, tensile strength (as brazed) and tensile strength (after aging) for the Nicrobraz alloy as compared with copper, S-590 stainless steel base metal and a high-temperature silver brazing alloy.

Tempil° Brochure

A new revised brochure covering the temperature indicating products manufactured by the Tempil° Corp. is now available.

The brochure contains concise directions for use; indicates the choice of temperature indicating product for the intended application; and lists the various temperature ratings available in each of the product types, including newly developed items in the 400 to 550° F interval.

Readers may obtain copies of it by requesting them on their business stationary.

Eutectic Literature

Several pamphlets are being made available to all readers by the Technical Information Service, Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, N. Y

A new 16-page "Rod Finder Guide" (T IS 1126) gives welding and plant foremen who plan and lay out work, a helpful source of "the right rod for the right job."

A new 32-page "DirectoRod Guide" (T IS 1340A) describes over 300 "low-heat input" metal joining applications and procedures.

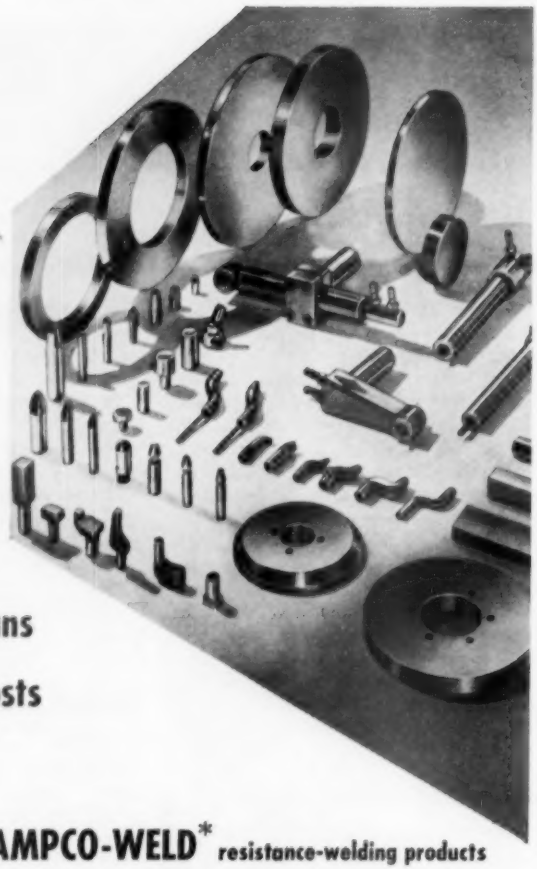
The new and expanded testing and technical information services offered industry following completion of new research laboratories are described in an anniversary issue of *Eutectic Welding News* (T IS 2463).

For your copy, write directly to the company at the above address.

Flux Kit

Alpha Metals, Inc., Jersey City, N. J., has produced a general-purpose soft soldering flux kit. It contains a complete set of the most useful fluxes, including printed circuit, electronic and general-purpose fluxes. According to the manufacturer, it enables a rapid determination of the proper flux for a soldering job and saves the trouble and time involved in acquiring samples. There are 16 fluxes in all. Each flux is contained in appropriately labeled glass bottles. The flap of the carton has a chart showing each flux in its position in the carton.

Bulletin is available upon request.



You've Got What It Takes

for longer runs and lower costs

...when you use AMPCO-WELD* resistance-welding products

Every item in the complete Ampco-Weld line is manufactured under strict laboratory control to give you longer runs and lower costs.

AMPCO-WELD spot-welding tips — stay cooler — won't stick to the work . . . tough — resist mushrooming and wear . . . fewer dressings needed — they last longer, cost less.

AMPCO-WELD rod — is available from stock — either in rounds, including hex, or in square and rectangular bars. There's a size for every need — all feature Ampco toughness and high electrical conductivity.

AMPCO-WELD seam-welding wheels — many users report 150% longer life. Resist wear — even at elevated temperatures. High electrical conductivity. Get them on short notice as rough forgings, finished blanks, or fully machined.

AMPCO-WELD resistance-welding holders — to suit every job — straight, offset, and universal ejector models. Offset and universal holders have interchangeable sockets for No. 1 and No. 2 Morse tapers. You change tapers without changing holders.

All Ampco-Weld resistance-welding products meet or exceed RWMA specifications. Experienced engineering service is also available. Get what it takes to lower your resistance-welding costs — get Ampco-Weld, the best value for your resistance-welding dollar.

RW-18

AMPCO METAL, INC.
Dept. WJ-6
Main Office and Plant • Milwaukee 46, Wis.
West Coast Plant • Burbank, California

AMPCO
Sole producer of genuine Ampco Metal

*Reg. U. S. Pat. Off.

Burdett Catalog

The Burdett Oxygen Co., of Cleveland, Ohio, has just issued its 1955 Catalog. Comprising 84 pages, it is one of the largest in the industry. Illustrated and described in detail are the latest types of equipment and supplies for both gas and electric welding. In addition, an entire section is devoted to industrial safety equipment.

The new Burdett Catalog H may be obtained by writing to the Burdett Oxygen Co., Department G, 3300 Lakeside Ave., Cleveland 14, Ohio.

Safety Catalog

Dockson Corp., 3829 Walbash, Detroit 8, Mich., has announced the publication of a new Head and Eye Protection Catalog.

The new catalog, S-55, contains the complete line of Dockson "better-built" head and eye protection equipment. Copies of the new catalog may be obtained by writing to the above address.

REVIEWS
OF NEW BOOKS

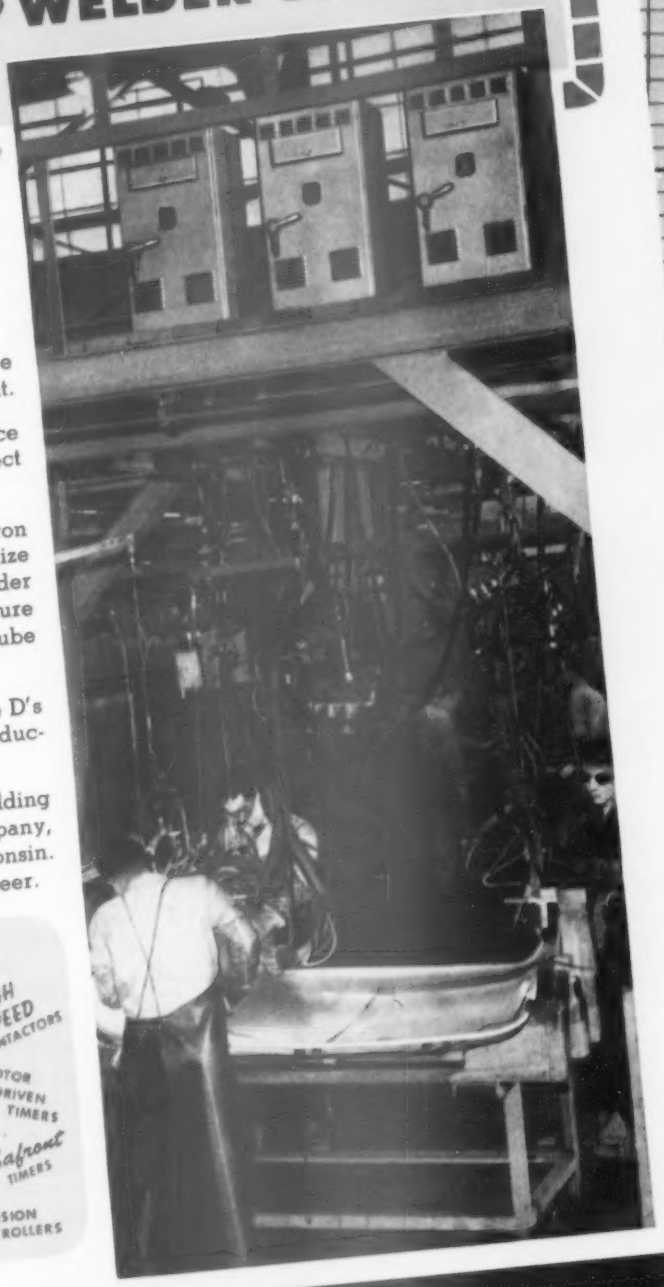
Oxy-Acetylene Welding

LA PRATIQUE DU SOUDAGE OXYACETYLÉNIQUE ET DES TECHNIQUES CONNEXES, by A. Leroy, M. Evard and G. D'Herbemont. Published by the French Institute of Welding, Paris (1955). 340 pages. (Reviewed by G. E. Claussen).

The authors of this book on oxy-acetylene welding and related processes are associated with the central office of the French Institute of Welding. The book is a comprehensive treatment of all oxy-acetylene processes with the strongly practical outlook suitable for an operator's manual. The reader is given detail instructions for carrying out every process. At the same time, he is given the reason behind each instruction.

The book is divided into five parts. Part I deals with the oxy-acetylene flame, generation of acetylene and oxygen, torches, regulators, and manifolds. Part II describes methods of welding, distortion, and weld testing. In Part III the weldability of steels and nonferrous alloys is discussed. Part IV deals with oxygen cutting, and Part V with brazing, surfacing, spraying, and pressure welding. In every part the information is detailed, quantitative and up to date.

LOWER PRODUCTION COST WITH SQUARE D *Electronic* WELDER CONTROL



New Cleveland Automotive Plant Uses Square D Control on Gun Welders

Square D control is paying off in three important ways in this new mass-production plant.

LESS INSTALLATION TIME was needed since the contactor, sequence-weld timer and disconnect are combined in one enclosure.

LESS MAINTENANCE because firing of ignitron tubes is synchronized with power line to minimize transients and reduce strain on cables and welder transformer windings. Fail-safe weld time feature eliminates possibility of burning work because tube failure stops weld immediately.

MORE CONSISTENT WELDS because Square D's lead-trail circuit feature insures full-cycle conduction and prevents transformer saturation.

Can Square D help you do a better welding job? Write for Bulletin 8992. Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin. Or talk to your nearby Square D Field Engineer.

The BEST Resistance Welder Control?

There is no one type. Square D builds both magnetic and electronic controls of many types—each best suited to certain types of applications. Specify Square D, and be sure of getting the best for your job.

HIGH SPEED CONTACTORS

MOTOR DRIVEN TIMERS

Safgrout TIMERS

Electronic TIMERS & CONTACTORS

SYNCRO-BREAK CONTACTORS

SYNCHRONOUS-PRECISION COMBINATION CONTROLLERS

SQUARE D COMPANY

How miller welders helped seal the arteries of America's largest single purpose power plant...

Substantial monument to Free Enterprise is OVEC and IKEC, suppliers of electric power to the Atomic Energy Commission's new uranium diffusion center in Pike County, Ohio.

Ohio Valley Electric Corporation and its subsidiary, Indiana-Kentucky Electric Corporation, agreed to supply electric power to this great new atomic production center . . . *15 billion kilowatt-hours annually.*

Two huge generating stations are being built: The Kyger Creek Plant at Cheshire, Ohio will have five generating units of 200,000 KW each; Madison, Indiana's Clifty Creek plant will produce 1,200,000 KW with its six big generators. Each of the eleven turbo-generating units will operate off a single boiler, with super-heated steam reaching 1,050 degrees Fahrenheit under 2,000 lbs. pressure per square inch! The high temperatures and reheat, which the sponsor companies have pioneered in their own right, will make these two plants among the most economical, most efficient generating stations in the country.

Miller Selenium Rectifier d-c Arc Welder, in four wide-range models. For complete, concise, worthwhile information on these and other Miller Arc and Spot Welders, contact us today . . . at no obligation, of course.

These same elements, however, make the fabrication of faultless high-pressure steam lines vital . . . and extremely difficult. To accomplish this extremely important task, pipe welding specialists equipped with the maximum of engineering skill utilized the Miller Selenium Rectifier d-c Arc Welder.

". . . if it's Miller, you *know* it's the *finest!*"

miller

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W. SPRAGUEN, DIRECTOR

WELDING RESEARCH COUNCIL
OF THE ENGINEERING FOUNDATION

SUPPLEMENT TO THE WELDING JOURNAL, JUNE, 1955

SOME WELDING RESEARCH PROBLEMS

The purpose of this compilation is to provide University Research Workers with a list of current welding research problems and to provide Project Committees with suggestions as to the needs of Industry. These suggestions have come largely from Industry

General Comments

About once in every two years the Welding Research Council canvasses industry as to their suggestions for problems which might be undertaken by the Project Committees of the Council and its University Research Committee.

Welding has become a world wide accepted method of fabrication, with a range of applicability from elements of the smallest electron tubes to the largest ships, tanks, bridges and buildings. Welding is used to join various metal parts including cast irons, plain carbon steels, low alloy and stainless steels, non-ferrous alloys of copper, nickel and aluminum; precious metals such as gold, silver, platinum and rhodium; and complex refractory alloys designed particularly for high temperature use. Sheets of the thinnest gage may be joined by one of the various welding processes or sections 20 in. thick and over. Welding enables the engineer to join together widely different metals at a reasonable cost and to utilize the desirable properties of each so that portions of a weldment may have superior resistance to abrasion, excellent corrosion resistance or good properties for high temperature or for low temperature service, while other parts of the weldment may have high tensile strength and ductility or some specific physical property such as low coefficient of thermal expansion or non-magnetic properties. Small wonder, then, that the problems created by the welding industry embrace practically every branch of science and engineering. Some of these problems are so complex that solution requires the combined wisdom of experts in many branches of sciences.

Obviously not all welding problems are suitable for University research projects on the undergraduate or even graduate level, but in this particular compilation the limitations of the Universities have been kept in mind.

Many of the problems can be investigated without welding equipment. Some require considerable outlays of time and equipment, but most can be subdivided or enlarged to suit the limitations of time and equipment of any research worker, short term or long term.

The central office of the Welding Research Council, 29 West 39 St., New York 18, N. Y., often can assist in outlining detailed research plans for anyone interested in one or more of the problems on the list. Sometimes, the Council can assist in supplying specimens and grants-in-aid. It is important to plan well ahead of the time at which the research can be started, because of unavoidable delays in delivery of specimens and appropriation of money. Planning should cover as many phases of the problem as possible in order to ensure that individual projects may contribute to a unified picture of the problem as a whole. A comprehensive and adequate investigation of any problem frequently requires a continuity of effort such as a series of student investigations, properly coordinated by an enthusiastic professor. The publication of progress reports of which the professor is co-author tends to rouse the interest of representatives of industry and to accord recognition to the university and professor.

To those universities who have been affiliated with the University Research Committee of the Council for periods up to thirty years, the utility of the list of suggested problems and the advantages of affiliation with

the Committee are well known. To those who are not familiar with the unusual opportunities afforded to welding researchers by affiliation with the Committee, one of its most important activities may be described.

A conference of University Research Professors is held each year during the National Metal Congress and Exposition. The Council makes available funds to pay for railroad and pullman expenses of professors who attend, with an upper limit of fifty dollars to any one professor or research worker. There is thus provided an opportunity for a university research worker to discuss his problems with leaders of industry and with other research workers in the field.

Through close connection with the AMERICAN WELDING SOCIETY, opportunity is provided for the presentation of results of a completed piece of research before the SOCIETY at the National Meetings either in the Spring or the Fall. Even though the paper may not be presented, an opportunity is provided for its publication in the "Welding Research Supplement." This "Supplement" accounts for about half the technical text pages of "THE WELDING JOURNAL," which reaches scientific workers throughout the world.

Suggested Programs

In a compilation of Research Problems that was prepared two years ago and published in the July 1953 issue of the "Welding Research Supplement," the suggestions received at that time were listed under five headings: Welding Physics and Chemistry, Welding Metallurgy, Mechanical Properties and Testing, Structural and Design, and Resistance Welding. This year they are being presented in somewhat different groupings as follows: Stainless Steel Weld Metal; Mild Steel Weld Metal; Brittle Behavior in Steels; Preheating vs. Postheating; Corrosion; Non-Destructive Tests; Anisotropy and Aircraft Problems. The thought behind this classification by "application" is that it may have a somewhat more direct appeal than the previous grouping and the prospective researcher may himself recognize the field in which his main interest lies.

Stainless Steel Weld Deposits

The advantages and disadvantages which may result from pre- and postheat treatment are not clearly understood. Although many specifications require or recommend postheat treatments, the resulting disadvantages often may outweigh the benefits gained. The effects of preheat treatments have also been misunderstood and should be further studied. It is not always clear whether all types of stainless steels should be treated in the same way. Similar considerations hold true for peening which, although often beneficial, may also cause detrimental effects.

The possible harmful effects of dilution should be further investigated. In particular, the behavior of various alloying elements in minimizing aggravating dilution effects should be clarified.

Buttering with 25-20 (Cr-Ni) electrodes is a procedure about which considerable controversy exists. Whereas some investigators feel that buttering prevents or considerably minimizes weld-metal cracking, others report that the use of 29-9 (Cr-Ni) electrodes and a standard multipass welding procedure is superior to buttering. These controversial reports should be investigated with respect to the effects of section size, amount of restraint, hardenability and composition of base steel, preheating, welding processes and procedure, electrode size, etc.[1]

EDITORIAL NOTE: There are also problems involving the loss of ductility in heat-affected zones either as a result of welding or post-heat treatments. There are also problems relating to the effect of ferrite and sigma phases on weld metal properties.

Mild Steel Weld Deposits

There is still need for further information on the study of weld metal produced in a concentrated atmosphere of hydrogen as is present when welding with atomic hydrogen, or with high cellulosic or hydrogen producing coatings. Study of weld metal produced under these conditions and compared with the weld metal produced in a hydrogen-free atmosphere may reveal information that is not presently available. It is not yet clearly established why very good results are generally obtained with atomic hydrogen shielded welding in view of the very detrimental effects of hydrogen with the metal arc.

Studies of the distribution of hydrogen in ferrite, martensite and austenite should also be desirable, particularly since no one theory has been universally accepted.

The problem of diffusible and nondiffusible hydrogen should also be studied further. It has been suggested that the quantity of diffusible hydrogen is inversely related to the iron oxide content of the weld metal. However, it might be more closely related to microfissures or porosity or inclusion content. Since diffusible hydrogen seems to be far more important than nondiffusible hydrogen, further investigations of the differences between them are advisable.

Studies should be made to determine the susceptibility of electrode coatings to moisture absorption under various atmospheric conditions of temperature and relative humidity. Although many studies have been made of the weld metal resulting from hydrogen and low-hydrogen bearing electrodes, little attention seems to have been given to finding out how the electrodes should be handled in use to avoid the troubles arising from possible moisture pickup. The relation of this characteristic of coating to the chemistry of the coating is a fundamental factor and little seems to have been published on this subject. Some information relative to proper methods of the restoration of electrode coatings would also be valuable. A study of this sort should include all types of commercial electrode coatings.

The cause of hot-cracking, associated with the presence of iron sulphide, should be further investigated. Why should low-hydrogen electrodes be more successful in avoiding hot-cracking in the welding of sulphur steels? There is certainly some evidence that carbon, silicon and hydrogen can play some part in the phenomenon.

The role of hydrogen in the formation of porosity is also not yet understood.[2]

Transition from Ductile to Brittle Behavior in Steels

Although a very large number of studies have been conducted during the last decade on the subject of transition temperature, there are still a great many gaps in technical knowledge. For example, there still is no generally accepted method for correlating the behavior of small-scale test specimens with the service behavior of large sections.

The effects of heat treatment, composition and structure need further study. Because pressure vessels are usually subjected to triaxial stresses and may be subjected to fluctuating pressures, and occasionally to shock or impact loads at low temperatures, the use and further improvement of materials and fabricating methods that produce or enhance notch toughness are highly desirable.

More information is desired on the effects of welding and the effect of preheat and postheat treatments. Also, little, is known about microfissuring and notch toughness of weld metal deposited under conditions of high restraint.

There is a lack of information on the effect of fabricating processes other than welding. Additional studies on the degree of restraint induced by thickness of the section might prove fruitful.

Although a large amount of work has been done on the effect of temperature on the properties of metals in the past 100 or more years, it is apparent that a great deal remains to be learned on both the technical and practical aspects of the problem.

It should, perhaps, be first noted that the evidence available in the literature suggests the rather broad conclusion that elastic properties (i.e., stiffness and yield strength) decrease in a more or less regular and predictable manner with increasing temperature, whereas those properties dealing with plastic deformation (hardness, strength, ductility or toughness), do not. The curves of these latter properties vs. temperature, in commercial steels, at least, are characterized by discontinuities and pronounced maxima and minima; the location of these peaks and valleys is markedly affected by such factors as chemical composition, steelmaking practice and heat treatment.

The behavior of steels at temperatures in the blue-heat region can be explained on the basis of strain aging caused by a precipitation phenomenon which interferes with plastic deformation resulting in an increase in hardness and strength and a decrease in toughness. This behavior can be controlled, within limits, by suitable deoxidation of the molten steel and heat treatment of the finished material.

A similar explanation has not been advanced to explain low-temperature brittleness, and the fundamental factors involved are not completely understood. The theory that it is "characteristic" for ferritic materials to become brittle at some temperature does not adequately account for the variations encountered in commercial steels or for the observed effects of composition, heat treatment, etc., on brittle behavior. Additional knowledge is needed to point the way to practical steps which might be taken to assure optimum properties in commercially produced material.

By way of example, the behavior of aluminum-killed steels within the temperature range under consideration is especially susceptible to change by heat treatment. It is known that the best combination of properties in such material is obtained after a normalizing heat treatment to refine the grain size. The desirable properties usually associated with aluminum deoxidation often are not obtained in the as-rolled condition although this is the condition in which large tonnages of pressure vessel and structural steels must be used. What, then, are the factors which influence the properties of aluminum-killed steels and what practical steps can be taken to improve the properties, or at least assure uniformity, of commercial hot-rolled material?

The magnitude of the shift in transition temperature resulting from the presence of weld reinforcement in one investigation was so much greater than anticipated, and the implications of the shift are of such far-reaching significance, that additional transitions should be determined for other weld and base-metal combinations. It is recommended that two commercial steels be used which represent the extremes of deoxidation practice—one rimmed and one fully killed. Although the behavior pattern of the E6010 deposit was consistent and readily understood in terms of notch toughness and flow strength, E6010 should be one of the weld metals investigated because of its widespread use in construction. The high-strength notch-tough E12016 deposit, on the other hand, should be investigated because when surface-ground it produced base-metal failures at temperatures considerably above the transition for prime plate.[3,4,5]

Preheating vs. Postheating

Investigations In Preheating

In connection with preheating, the important practical questions are: when must preheating be used, what temperature is required and when by preheating may postheating be eliminated? While these questions gradually (but sometimes painfully) are answered by experience, experimental data would be helpful if they would provide answers to certain allied problems such as the following:

1. The relation of preheating to distortion and cracking:
   - (*a*) What quantitative effect has preheating on reaction (general) stresses and local residual stresses?
   - (*b*) Does preheating diminish weld cracking at high temperatures (auto-cracking)?
   - (*c*) Is there a definite minimum temperature for each grade of steel that may be relied upon to prevent underbead cracking?

2. The relation of preheating to notch toughness:
   (*a*) Comparison of preheated vs. postheated weldments of various pressure vessel steel grades.
   (*b*) How important is it to preheat to avoid microcracks, under what range of conditions do they form and how much effect have they on notch toughness?
   (*c*) Effect of preheat temperature on the notch toughness of lower carbon steels (say, below 0.18% C).

Investigations in Postheating

The practical questions which concern postheating are: how beneficial it is, and how high must the weldment be heated, also how much of the weldment must be heated? Experimental data are needed on the following points:

1. Localized vs. general postheat. How much of the weld zone must be heated to obtain the desired metallurgical effects? Are there any detrimental effects (residual stress) to localized preheat?
2. Quantitative relations between notch-toughness and the time and temperature of postheating.
3. More conclusive data on the effects of postheating on the properties of weld metal, particularly for higher strength types.
4. The relative importance of stress-relief and metallurgical tempering as means of improving notch toughness of welded pressure vessel steels.
5. Strain-aging effects during postheating in both weld metal and base metal.
6. See also Item 2-a under preheating problems.[6]

Corrosion

Economics make it no longer acceptable to hide ignorance by ample overdesign to take care of the unknown in corrosion. Therefore, it becomes important to learn more about corrosion theory and corrosion mitigation.

In neutral solutions, the corrosion of iron is controlled largely by the amount of oxygen available. Small changes in composition, type of heat treatment, surface preparation and internal stresses have little or no effect on over-all weight loss. However, such factors can localize the attack and promote pitting or cracking. This aspect needs further study. Perhaps one way of doing this is to study the electrochemistry involved, to determine the potentials of local anodes and cathodes the area ratios and the polarization curves. Similar remarks apply to other alloy systems as well as iron and steel.

In acid solutions, hydrogen evolution is the major cathode reaction and probably controls the corrosion. Here the behavior of the minute cathodes on the metal surface becomes dominant. More needs to be known about hydrogen overvoltage, and the effects of surface finish, inclusions, alloying elements, surface films and corrosion products. What determines whether the hydrogen will enter the metal or be evolved as a gas?

At present corrosion is prevented by cathodic protection, by metal coatings, by organic coatings, by inhibitors, by treatment of the environment and by the use of alloys. But, as indicated in the text, the application of these methods to the prevention of cracking needs further study.

Cathodic protection applied too late, for example, may not stop the growth of cracks already existing. Underprotection may actually localize the attack and hasten a failure due to cracking. Overprotection with too much current may cause hydrogen embrittlement and resultant complications. Some easy and satisfactory means of establishing when complete cathodic protection is attained is urgently needed. Also needed are quantitative data relating the corrosion factors of the environment (temperature, aeration, velocity, pH, dissolved salts, galvanic couples, time, etc.) to the minimum current for total protection. Substantially similar remarks could be made for the use of inhibitors. A better understanding of the mechanism of their action and of their proper application is needed.

Returning specifically to stress-corrosion cracking, there is no way of knowing whether an alloy will resist cracking in a given environment except by trial and error. It is not known why some environments produce cracking and others do not. The theory and mechanism of stress corrosion cracking needs further development with detailed knowledge of the influence of internal structure, the magnitude and distribution of stresses, and the influence of the environment. How do minor constituents in the environment act as accelerators or as inhibitors of cracking? What determines whether the path of the crack will be intergranular or transcrystalline? Also the roles, if any, of hydrogen in stress corrosion cracking should be tied down. More needs to be known about the effect of changes in composition, heat treatment and structure of metals and alloys. It is not yet certain whether a susceptible alloy can be improved, purified or modified to make it acceptable under some conditions.

With reference to corrosion fatigue much more data are needed under actual service conditions. Corrosion fatigue limits vary with the corrosive environment, but with a sufficient background of information it should be possible to make intelligent appraisals concerning behavior in new environments. Quantitative data are needed relating corrosion fatigue limits to the corrosive factors of the environment, such as temperature, aeration, velocity, pH, etc. More information is needed also on the various protective measures which have been suggested. The theory needs further development particularly as to mechanism by which stress accelerates corrosion. Measurements of the potentials of local anodes and cathodes, area ratios and polarization curves would be elucidating.

The factors which control the entrance of hydrogen into a metal are not well understood. Just how the hydrogen behaves after it gets in the metal is also not clear, although it is certain that its effects can be damaging and can promote cracking. This whole subject of hydrogen damage in aqueous solutions needs further

study, particularly where it ties in with cracking of pressure vessels.[7]

Nondestructive Tests

These recommendations are based upon indications that improvement in techniques and methods of nondestructive flaw detection are highly possible.

1. Improvement in existing radiographic methods: (*a*) Encourage X-ray equipment manufacturers to develop lighter units that are highly portable and possess the rugged features required of equipment used in field inspection of ship structures. By reducing setup and maintenance time, such equipment would allow a higher rate of inspection per unit. (*b*) Establish a program to explore the potentialities of iridium-192 and other promising radioactive isotopes. An adequate source of supply of those isotopes found applicable must be established. Data must be secured to compare their quality with established radiation sources for the inspection of welds in various thicknesses of steel plate. (*c*) Develop standards based on research and service data. These standards should clearly show the maximum size of each type of defect that may be allowed to remain in a weld without the probability of triggering a brittle fracture.

2. Improvement of filmless techniques: (*a*) Promote the development of fluoroscopy to yield better sensitivity for thinner sections. Study the applicability of the method to the inspection of welds in ship structures. (*b*) Review developments in other filmless techniques, such as the "Picker-Polaroid-Land" process and the Westinghouse "Fluorex" process from time to time to determine if further research has made any of these methods applicable to weld inspection in ship structures. (*c*) Investigate the applicability of xeroradiography for weld inspection in steel. Xeroradiography must be improved to consistently give the maximum sensitivity required for ship structure inspection. Further, the optimum radiation source must be established for this process.

3. Investigation of magnetic-particle method: The magnetic-particle method should be thoroughly evaluated to determine its ability to consistently detect sub-surface defects in welds of a given size, type and location. Standards must be developed for this method.

4. Improvement of the ultrasonic method: Improvements and modifications must be made on equipment for the ultrasonic method before it can be adapted to weld inspection on ship structures. Further, the method must be fully evaluated to determine its reliability to detect given flaws in welds.

5. Improvement of the penetrating oil method: The so-called whiting and the zyglo methods still leave something to be desired in a method for inspecting non magnetic materials that will be comparable to the magnetic particles method for magnetic materials.[8]

Anisotropy

It is evident that there are many gaps in the information on the effects of anisotropy. More information on the effect on directionality of temper embrittlement, hydrogen embrittlement and prestrain is needed. Also, there is little information on the effect of welding on directionality, particularly in the heat-affected zone. In addition, some attention should be given to a more direct correlation of steelmaking practice to anisotropy.[9]

Aircraft Problems

A number of problems have been submitted to the Aircraft Advisory Committee of the Welding Research Council as needing investigation. For the most part these problems are not suitable for thesis work in connection with University research. A few of them are worthy of consideration in this field. Some of these are given below. The University Research Committee will be glad to assist in the formulation of any of these problems:

1. Evaluation of fusion weldability of available high strength, heat treatable aluminum alloys, such as 24S, 14S and 75S.

2. Evaluation of fusion weldability of low-alloy, heat treatable steels containing Boron.

3. Evaluation of fusion weldability of the presently available magnesium alloys.

4. Evaluation of soft solders for use in electrical applications that are suitable for use at temperatures of 300 to 500° F, in applications operating continuously.

5. Development of techniques to fabricate brazed sandwich construction for use in applications over 1000° F.

6. Evaluation of fusion, pressure, flash, spot and seam welds in commercially pure titanium and titanium alloys.

7. Develop a technique which is practical for shop usage for brazing and soldering commercially pure titanium and its alloys.

8. The establishment of design criteria for spot and seam welded joints in aluminum, magnesium and corrosion and heat resistant alloys.

9. Fatigue Properties of Aircraft Quality Arc Welds. Fatigue failures have been a constant source of difficulty in the use of arc-welded structures. Very little factual data are available on the fatigue properties of arc welds in low-alloy steels. Data are required for welded joints used in the "as-welded" condition, for joints heat treated before welding and left in the "as-welded" condition, and for joints heat treated after welding to various strength levels. During the past several years, considerable improvement in welding electrodes has been achieved. The most promising of these electrodes should be selected and used and evaluated in this development program.

10. Evaluation of tensile/shear strength ratios as a measure of spot weld ductility.

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CARBON PICK-UP IN CARBON-ARC GROOVES

BY B. M. MacLEOD

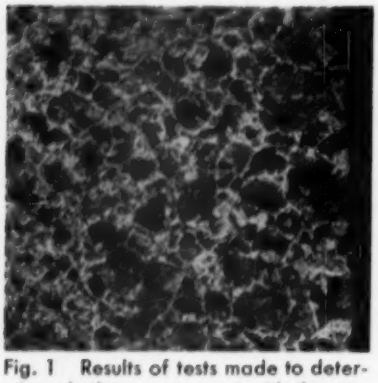

Fig. 1 Results of tests made to determine whether grooves cut with the carbon arc torch become carburized. This view shows a cross section of a groove cut with full air pressure. Average Vickers hardness in the pearlite is 221 (Brinell 221). X 100

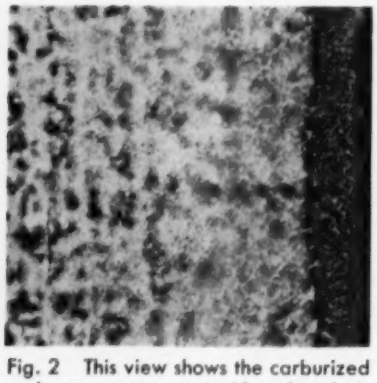

Fig. 2 This view shows the carburized scale on a groove cut with reduced air pressure. Average Vickers hardness in the pearlite is 207 (Brinell 197). Average Vickers hardness in the carburized edge is 458 (Brinell 437). X 100

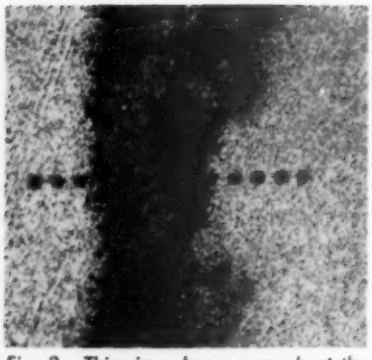

Fig. 3 This view shows a crack at the interface of a carburized groove and the weld metal. Left to right, the average hardness is: plate, Vickers 196 (Brinell 187); carburized zone, Vickers 330 (Brinell 309); weld metal, Vickers 237 (Brinell 231). X 100

One of the relatively new metal-removing tools consists of a torch that holds a carbon-electrode and has an air-control valve in the handle. As the carbon-arc melts the metal, a compressed-air stream blows away the molten metal.

We conducted tests to determine if grooves cut with the carbon-arc torch become carburized. We suspected that reduced air-pressure from a partially depressed air-valve handle could cause trouble. To check this, we cut grooves in low-carbon steel plate with the air-valve handle fully depressed and half depressed.

The torch uses line air-pressure of 80 to 90 lb. We found that when full line air pressure is used, there is no carburization. But when the air-valve handle is half depressed, a reduced air pressure results that leaves the groove surface carburized. If this carburized zone is not washed out during welding, it may cause cracking of the weld metal. Results of the tests are illustrated in Figs. 1 through 3.

When operating with a partially open air valve, not all of the molten metal is blown away. The thin molten layer that is left, picks up carbon from the arc and solidifies. After the groove is cut, the surface appearance shows whether sufficient air pressure has been used. A non-carburized groove has a dull sheen. Carburized grooves have no sheen.

Carburizing can occur when the operator does not fully depress the air-valve handle. A crimped air hose or dirt in the line may cause the same effect.

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A MATHEMATICAL ANALYSIS OF THE TEMPERATURE DISTRIBUTION DURING FLASH WELDING

Authors propose methods for choosing an adequate mean value of thermal diffusivity for a given material and for evaluating the amount of critical burn-off necessary to establish the stabilized temperature distribution

BY E. F. NIPPES, W. F. SAVAGE, H. SUZUKI AND W. H. CHANG

ABSTRACT. A mathematical analysis of the flashing operation as well as the nature of the stabilized temperature distribution established during flashing has led to the introduction of a new set of universal parameters with which the temperature distribution resulting from any given flashing pattern in a material may be correlated with a single, standard distribution form. The validity of such a distribution form for parabolic flashing has been satisfactorily verified by the experimental data obtained for aluminum alloys, steels and molybdenum.

The effect of flashing variables, such as material constants, specimen size, rate of platen movement and initial clamping distance, on the stabilized temperature distribution has been discussed.

Methods are proposed for choosing an adequate mean value of thermal diffusivity for a given material and for evaluating the amount of critical burn-off necessary to establish the stabilized temperature distribution. Examples are given for predicting temperature distributions and predetermining satisfactory flashing conditions.

It is hoped that the present analysis will not only contribute to the understanding of the flashing operation and the nature of the temperature distribution, but also eliminate much of the costly experimental work hitherto necessary in establishing optimum flashing conditions.

1. Introduction

In order to determine optimum flash welding conditions, the temperature distribution established during flashing has been investigated extensively at the Welding Laboratory of Rensselaer Polytechnic Institute for structural aluminum alloys,[1] SAE 1020 steel[2, 3] and sintered molybdenum.[4] In these investigations two types of flashing pattern, parabolic and linear, have been employed. In the parabolic pattern, the burn-off velocity increases with time at a constant rate and, accordingly, the burn-off increases with time parabolically. In the linear pattern, the burn-off velocity is kept constant throughout the flashing cycle.

It has been found from these investigations that in parabolic flashing the average temperature of the flashing interface soon reaches and remains at the melting point of the specimen. Accordingly, a dynamic thermal equilibrium is maintained between the rates of heat input and heat dissipation, leading to a stable temperature distribution over a considerable range of burn-off. The gradient of this temperature distribution is found to be steeper the higher the platen acceleration and the shorter the initial clamping distance. In linear flashing, on the other hand, such a stable condition of temperature distribution has not been observed except for long clamping distances.[2]

It has long been realized at this Laboratory that the temperature distribution in any material may be predicted if a simple relationship can be established among material constants and flashing variables. Toward this end, a first trial was made in the parabolic flashing of aluminum alloys,[1] in which the temperature distribution was found to be approximated by an inverse exponential function of a composite variable Vx/k, where V is the instantaneous velocity of burn-off per specimen, x the instantaneous distance from the flashing interface and k the thermal diffusivity of the material being flashed. Unfortunately, the choice of a representative value of V for the composite variable was difficult because in parabolic flashing, the velocity is time-dependent.

Considerable success was achieved in the case of parabolic flashing of steels. Based on the experimental results,[2, 3] curves were obtained representing the stabilized temperature distribution as a function of an empirical parameter P, which was defined by

$$P = \frac{\text{Platen acceleration} \times \text{Thickness}}{\text{Initial clamping distance}}$$

However, since P does not contain any material constants, these curves are useful only for steels and, for a different material, a different group of curves must be prepared.

The present mathematical analysis has been performed to overcome the above-mentioned difficulties. With the new flashing parameters introduced in this paper, all temperature distributions resulting from a given flashing pattern can be correlated by means of a single, standard distribution form, regardless of material or flashing conditions. As will be shown later, such a correlation has been verified satisfactorily in the case of parabolic flashing for which enough experimental data are available. It is the ultimate purpose of this paper that the proposed universal distribution forms may be used in predicting temperature distributions resulting from the appropriate flashing pattern, thereby permitting the selection of optimum flashing conditions for any material without the expenditure of experimental work.

2. Dimensional Analysis of Flashing Variables

A dimensional analysis of the pertinent material constants and flashing variables provides a clue to the required flashing parameters. This analysis is based upon the assumption that the flashing interface is the only heat source and that the clamping distance is infinite so that the effect of cooling by the clamping jaws can be completely neglected (the effect of finite clamping distance is discussed later in §5). Further, since the heat loss from the specimen into the surroundings is not appreciable, the temperature distribution in

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the specimen is assumed to be unidimensional along the specimen axis.

In the theory of heat flow, it is the temperature increase, $T - T_0$, from the initial temperature T_0, rather than the absolute value of the temperature T, that commands significance. Since this temperature increase can never exceed the maximum value $T_m - T_0$, where T_m is the melting point of the specimen, the following identity constitutes a convenient expression for the nondimensional temperature:

$$\psi \equiv \frac{T - T_0}{T_m - T_0} \qquad (1)$$

It is obvious that one of the variables which determine the heat flow, and thus the nondimensional temperature, is the thermal diffusivity k of the specimen, defined as $k \equiv K/\rho c$, where K is the thermal conductivity, ρ, the density and c, the specific heat of the specimen. As a first approximation, k is taken as a constant for a given material, whereas in the final analysis, its variation with temperature must be considered, as discussed in Appendix C. A second variable which influences the temperature distribution is the burn-off (the motion of the flash-welder platen) which controls the primary source of heat input. In the present case where only the stabilized temperature distribution is of interest, the parameter chosen to describe the platen movement should be time-independent, such as the platen velocity in linear flashing or the platen acceleration in parabolic flashing. Finally, since the temperature of a point in the specimen varies with the location of the point with respect to the flashing interface, the instantaneous distance from the interface should also be included in the analysis.

Therefore, to determine the stabilized temperature distribution established in a semi-infinite solid by a given flashing pattern, the following three variables must be considered:

k = thermal diffusivity (in.^{2}/sec).
ξ = instantaneous distance from flashing interface (in.).
g_p = platen acceleration (in./sec^2) in parabolic flashing.

or

V_p = platen velocity (in./sec) in linear flashing.

According to the theory of dimension, the nondimensional temperature ψ should be a function of a nondimensional parameter P which is composed of these three variables for each type of flashing pattern, i.e.,

$$\psi = f(P) \qquad (2)$$

It is the immediate objective of this analysis to derive the forms of P.

Parabolic Flashing

Assume the nondimensional parameter P to be of the form

$$P = g_p{}^{\alpha} k^{\beta} \xi^{\gamma}$$

Since the dimensions of the variables are, respectively,

$$(g_p) = (L/T^2), \quad (k) = (L^2/T), \quad (\xi) = (L)$$

where L and T represent the dimensions of length and time, respectively, the dimension of P is given by

$$(P) = (L^{\alpha+2\beta+\gamma})/(T^{2\alpha+\beta})$$

As P should be dimensionless, it follows that

$$\alpha + 2\beta + \gamma = 0 \quad \text{and} \quad 2\alpha + \beta = 0$$

or

$$\alpha = \gamma/3 \quad \text{and} \quad \beta = -2\gamma/3$$

from which

$$P = [(g_p/k^2)^{1/3}\xi]^{\gamma}$$

Therefore, the simplest form of the nondimensional parameter for the parabolic flashing, designated by η, is

$$\eta \equiv (g_p/k^2)^{1/3}\xi \qquad (3)$$

which is henceforth called the instantaneous-equivalent distance.

Linear Flashing

With a similar reasoning, the expression

$$\lambda \equiv (U_p/k)\xi \qquad (4)$$

is obtained as the equivalent distance for the linear flashing pattern, where U_p is the constant platen velocity.

General Flashing Pattern

In a general flashing pattern in which the platen displacement X_p is related to time t, by

$$X_p = at^n \qquad (5)$$

where a and n are arbitrary constants, the generalized form of the instantaneous-equivalent distance is

$$P = (na/k^n)^{1/(2n-1)}\xi \qquad (6)$$

As special cases, this relation gives:

for $n = 1$ (linear):

$$P = \lambda = (a/k)\xi = (U_p/k)\xi$$

for $n = 2$ (parabolic):

$$P = \eta = (2a/k^2)^{1/3}\xi = (g_p/k^2)^{1/3}\xi$$

for $n = 3$ (cubic):

$$P = \omega = (3a/k^3)^{1/5}\xi$$

. . . , etc.

3. Results of Mathematical Analysis

Although the theory of dimension predicts $\psi = f(P)$, the functional form of this relationship cannot be determined by the dimensional analysis, but must be obtained experimentally or theoretically. In the appendices, a generalized equation (eq 36, Appendix G) expressing such a relationship has been derived from a simplified mathematical analysis of the flashing operation. From this generalized equation, individual equations for different flashing patterns can be obtained readily.

In the case of parabolic flashing (Appendix J), the theoretical functional form is

$$\psi_\infty = f(P) = e^{-1.5\eta} \qquad (7)$$

where $\eta \equiv (g_p/k^2)^{1/3}\xi$, presented previously as eq 3, and the subscript ∞ denotes the condition of infinite clamping distance. As will be shown subsequently, all experimental data on parabolic flashing, when expressed nondimensionally and corrected to a standard form, conform satisfactorily to the proposed experimental equation

$$\psi_\infty = f(P) = e^{-0.92\eta} \qquad (8)$$

irrespective of specimen material or flashing conditions. The discrepancy between eqs 7 and 8 may be attributed to the assumptions, stated in the appendices, which have been made in the derivation of the generalized equation.

From eq 8 the following conclusions may be drawn:

1. The nondimensional temperature ψ_∞ is a function of the nondimensional parameter P as predicted.
2. The choice of eligible variables in deriving the form of P is correct.
3. Equation 8, simple as it is, serves as a universal relationship from which the stabilized temperature distribution established in parabolic flashing of any material can be predicted.

For linear flashing, the theoretical stabilized temperature distribution is derived to be

$$\psi_\infty = e^{-0.77\lambda} \qquad (9)$$

where $\lambda \equiv (U_p/k)\xi$. As mentioned previously, a stabilized temperature distribution was observed only in a few cases where long clamping distances were used. These limited data, when expressed nondimensionally and with the cooling effect of the clamping jaws corrected, show considerable scattering in the plotting of ψ_∞ versus λ. The curve drawn through the average of these experimental points follows the equation

$$\psi_\infty = e^{-0.5\lambda} \qquad (10)$$

While it is uncertain whether eq 10 represents the true experimental relationship for linear flashing, the equation is nevertheless given here for future reference. It is interesting to note that the ratio of exponents, 0.77/0.5 (= 1.54) from eqs 9 and 10 is almost identical with that of 1.5/0.92 (= 1.63) from eqs 7 and 8 for parabolic flashing.

4. Stabilized Temperature Distribution for an Infinite Clamping Distance

In the parabolic flashing of aluminum alloys,[1] it was found that for clamping distances exceeding a certain critical value, the temperature distribution was not appreciably altered by further increase in clamping distances. It is therefore interesting to compare the temperature distributions obtained with

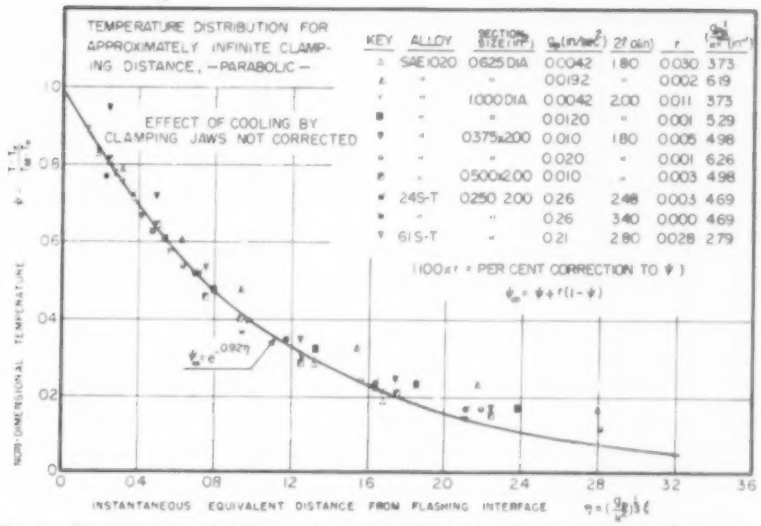
TEMPERATURE DISTRIBUTION FOR APPROXIMATELY INFINITE CLAMPING DISTANCE, —PARABOLIC—
EFFECT OF COOLING BY CLAMPING JAWS NOT CORRECTED
KEY ALLOY SECTION SIZE (IN.) g_p (IN/SEC²) 2T (SEC) r $\frac{g_p}{\alpha^2}$ (in⁻¹)
SAE 1020 0.625 DIA 0.0042 180 0.030 373
" " 0.0192 " 0.002 619
" " 1.000 DIA 0.0042 200 0.011 373
" " 0.0120 " 0.001 529
" " 0.375x2.00 0.010 180 0.005 498
" " 0.020 " 0.001 626
" " 0.500x2.00 0.010 " 0.003 498
24S-T 0.250 2.00 0.26 248 0.003 469
" " 0.26 340 0.000 469
61S-T " 0.21 280 0.028 279
(100r = PER CENT CORRECTION TO ψ)
$\psi_\infty = \psi + r(1-\psi)$
$\psi = e^{-0.92\eta}$
NON-DIMENSIONAL TEMPERATURE
INSTANTANEOUS EQUIVALENT DISTANCE FROM FLASHING INTERFACE
$\eta = (\frac{g_p}{k^2})^{1/3}\xi$

Fig. 1 Temperature distribution for approximately infinite clamping distance in parabolic flashing

long clamping distances with those which would be obtained with an infinite clamping distance.

Figure 1 is a collection of ten temperature distributions obtained with parabolic flashing of the aluminum alloys, 24S-T and 61S-T, of rectangular section, and of SAE 1020 steel of rectangular and round sections. The nondimensional temperature ψ as observed is shown versus the instantaneous-equivalent distance η.

Since the thermal diffusivity of steel varies considerably with temperature, a preliminary study was undertaken to determine the most adequate value of k, the thermal diffusivity, which should be used in computing η (Appendix C). As a result, the value of k = 0.0090 in.²/sec was chosen as the thermal diffusivity of SAE 1020 steel. The melting temperature T_m of the steel was taken equal to the mean value of the liquidus and solidus, or T_m = 2750° F.

For aluminum alloys, the thermal diffusivity and melting temperature were taken as follows (Appendix C):

for 24S-T:

k = 0.078 in.²/sec, T_m = 1057° F

for 61S-T:

k = 0.098 in.²/sec, T_m = 1142° F

The initial temperature of the specimen was taken as T_0 = 80° F throughout this paper.

In Fig. 1 the solid curve represents the experimental equation corrected to infinite clamping distance,

$$\psi_\infty = e^{-0.92\eta}$$

It is apparent that in spite of the wide variations in thermal diffusivity and platen acceleration, the correlation between this curve and the experimental points is rather satisfactory. From this the conclusion follows that the temperature distributions obtained with long clamping distances are approximately the same as those which would be obtained with an infinite clamping distance.

5. Effect of Finite Clamping Distance

In view of the cooling effect of the water-cooled copper clamping jaws, the effect of a finite clamping distance will be to lower the temperature distribution curve below that for an infinite clamping distance. If it is assumed that the temperature of the specimen is always kept at T_0 at a certain distance l_1 from the flashing interface, it can be shown mathematically (Appendix K) that for parabolic flashing, the cooling effect can be corrected through the formula

$$\psi_\infty = \psi + r(1 - \psi) \qquad (11)$$

where ψ is the nondimensional temperature obtained experimentally, ψ_∞ the standard nondimensional temperature corrected to an infinite clamping distance and r the correction factor. In deriving eq 11, the following relationships have been employed:

$$r = e^{-0.92\eta_1} \qquad (11a)$$

$$\eta_1 \equiv (g_p/k^2)^{1/3} l_1 = (g_p/k^2)^{1/3}(l_0 - B_c + \nu D) \qquad (11b)$$

where

ν = 1 for rectangular section.
ν = 1/2 for round section.
$\nu = \frac{D^2 - d^2}{2D^2}$ for tubular section of OD = D, and ID = d.
D = diameter or thickness of specimen.
l_0 = 1/2 × (initial clamping distance).
B_c = critical burn-off per specimen.

As shown in §7, the critical burn-off per specimen, defined as the minimum amount of burn-off per specimen required to raise the average temperature of the flashing interface to the melting temperature of the specimen, may be evaluated by

$$(g_p/k^2)^{1/3} B_c = 0.70 \qquad (11c)$$

Inspection of eq 11*a* indicates that the correction factor r is smaller the larger the value of η_1. It follows from eq 11*b* that the nondimensional temperature will be less affected by the cooling effect of the clamping jaws under the condition(s) of higher platen acceleration, smaller thermal diffusivity, longer clamping distance or greater specimen thickness. These deductions are in agreement with the experimental results some of which were mentioned in the introduction of this paper.

If eq 10 is assumed to be the true experimental relationship for linear flashing, a similar formula for correcting a measured temperature to the standard form will then be

$$\psi_\infty = \psi + r(1 - \psi) \qquad (12)$$

where

$$r = e^{-0.5\lambda_1} \qquad (12a)$$

$$\lambda_1 \equiv (U_p/k)(l_0 - B_c + \nu D) \qquad (12b)$$

and U_p = platen velocity

As shown in §7, the critical burn-off per specimen may be obtained from

$$(U_p/k)B_c = 1.82 \times 0.70 = 1.27 \qquad (12c)$$

In parabolic flashing, it has been established experimentally that, within certain limits, a given temperature distribution may be produced under different combinations of platen acceleration and initial clamping distance. Analytically, this can be treated from the foregoing relationships as follows:

From eq 11,

$$\begin{aligned} r &= (\psi_\infty - \psi)/(1 - \psi) \\ &= (e^{-0.92(g_p/k^2)^{1/3}\xi} - \psi)/(1 - \psi) \\ &= (e^{-0.92G\xi} - \psi)/(1 - \psi) \end{aligned} \qquad (13)$$

where $G \equiv (g_p/k^2)^{1/3}$.

From eq 11*a*,

$$\begin{aligned} r &= e^{-0.92\eta_1} = \\ &\quad e^{-0.92[(g_p/k^2)^{1/3}(l_0 + \nu D) - 0.70]} \\ &= e^{-0.92(GL - 0.70)} = \\ &\quad 1.904e^{-0.92GL} \end{aligned} \qquad (14)$$

where $L \equiv l_0 + \nu D$.

Combining eqs 13 and 14 gives

$$e^{-0.92G\xi} - \psi = 1.904(1 - \psi)e^{-0.92GL} \qquad (15)$$

The use of eq 15 in determining different combinations of flashing conditions to produce an identical temperature distribution in a given material is illustrated in §8.

6. Stabilized Temperature Distribution in Parabolic Flashing with Finite Clamping Distance

In this section, a correlation of the extensive temperature distribution data obtained with parabolic flashing pattern at the Welding Laboratory of Rensselaer Polytechnic Institute will be discussed. It may be mentioned that these temperature distributions were meas-

ured by thermocouples welded into a series of holes drilled in one of a pair of specimens. The experimental error was, in general, of the order of ±50° F and may be larger in some special cases.

Aluminum Alloys of Rectangular Section

In the report of an early investigation of the flash welding of aluminum alloys, in which a parabolic pattern was used,[1] only the instantaneous velocities of the flashing interface, rather than the platen accelerations, were listed. In this section, therefore, the value of platen acceleration has been computed from

$$g_p = V^2/B$$

where V, the instantaneous velocity of the flashing interface, is equal to half the instantaneous platen velocity V_p, and B, the corresponding burn-off per specimen, is taken as equal to half the total platen displacement X_p.

For the alloys studied, the material constants (Appendix C) are as follows:

Alloy	*Melting range,* ° *F*	*Melting point,** ° *F*	*Thermal diffusivity,* $in.^2/sec$
14S-T	950–1180	1065	0.096
24S-T	935–1180	1057	0.078
61S-T	1080–1205	1142	0.098

* Taken as the mean value of the melting range.

Table 1 gives the temperature distributions from the original report[1] expressed in the terms developed in the foregoing sections. Figure 2 shows the uncorrected nondimensional temperature distributions obtained from these data, as affected by the finite clamping distance. A considerable scattering is immediately noticed. However, when these data are corrected to the standard form through eq 11, a far better consistency is obtained, as may be seen from Fig. 3.

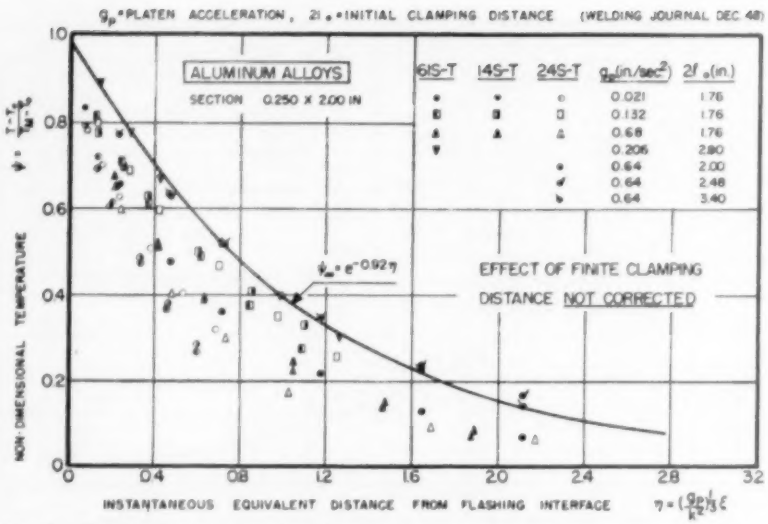

Fig. 2 Effect of finite clamping distance on experimental temperature distribution in parabolic flashing of aluminum alloys

SAE 1020 Steel of Rectangular Section

Table 2 contains the stabilized temperature distributions[2] obtained for SAE 1020 steel of rectangular section of three thicknesses, 0.250 x 2.0 in., 0.375 x 2.0 in. and 0.500 x 2.0 in.

The standard temperature distribution for the 0.250-in. section is presented in Fig. 4. The values of the correction factor r, listed in both Table 2 and Fig.

Table 1—Experimental Temperature Distributions in Parabolic Flashing of Aluminum Alloys. From Data of Curran, Patriarca and Hess, The Welding Journal, 27(12), Research Suppl., 577-s–592-s (1948). (Specimen size: 1/4 x 2-in.)

Alloy	Mean flashing velocity in./sec.	Platen acceleration g_p in./sec²	Initial clamping distance $2(l_o)$ in.	$(\frac{g_p}{k^2})^{\frac{1}{3}}$ $in.^{-1}$	Experimental temperature distributions: ψ = Nondimensional temperature values; η = Nondimensional distance values; ξ = Instantaneous distance from flashing interface, in.	0.05	0.10	0.15	0.25	0.35	0.45	Correction factor r
	0.053	0.021	1.76	1.32	ψ	.782	.690	.609	.477	.365	.285	0.482
					η	.066	.132	.197	.329	.460	.592	
14S-T	0.132	0.132	1.76	2.42	ψ	.772	.690	.619	.497	.406	.330	0.154
					η	.121	.242	.363	.605	.847	1.088	
	0.30	0.68	1.76	4.19	ψ	.650	.507	.386	.244	.142	.080	0.024
					η	.209	.419	.628	1.047	1.468	1.885	
	0.053	0.021	1.76	1.51	ψ	.778	.697	.625	.502	.400	.317	0.394
					η	.075	.151	.227	.378	.529	.680	
	0.132	0.132	1.76	2.77	ψ	.799	.686	.594	.461	.348	.255	0.108
					η	.138	.277	.415	.693	.970	1.245	
	0.30	0.68	1.76	4.81	ψ	.593	.400	.297	.175	.092	.065	0.013
24S-T					η	.240	.481	.722	1.021	1.682	2.165	
	0.26	0.64	2.00	4.69	ψ	.656	.472	.358	.214	.132	.070	0.009
					η	.234	.469	.704	1.172	1.640	2.108	
	0.26	0.64	2.48	4.69	ψ	.763	.625	.512	.348	.225	.142	0.003
					η	.234	.469	.704	1.172	1.640	2.108	
	0.26	0.64	3.40	4.69	ψ	.767	.625	.513	.348	.225	.142	0.000
					η	.234	.469	.704	1.172	1.640	2.108	
	0.053	0.021	1.76	1.31	ψ	.830	.716	.612	.470	.367	.263	0.487
					η	.066	.131	.196	.327	.457	.590	
	0.132	0.132	1.76	2.40	ψ	.812	.706	.612	.490	.376	.272	0.157
61S-T					η	.120	.240	.360	.600	.840	1.080	
	0.30	0.68	1.76	4.16	ψ	.678	.507	.386	.225	.140	.075	0.025
					η	.208	.416	.628	1.040	1.455	1.870	
	0.21	0.206	2.80	2.79	ψ	.886	.776	.667	.517	.396	.300	0.028
					η	.139	.279	.418	.698	.977	1.255	

4, are generally smaller than those for aluminum alloys in Table 1. This is probably due to the small thermal diffusivity of steel as compared with that of the aluminum alloys (about one-tenth).

The corrected temperature distribution for this thin, 0.250-in. section shown in Fig. 4 appears somewhat higher than the standard distribution curve obtained from

$$\psi_m = e^{-0.92\eta}$$

This discrepancy is believed to have resulted from a slightly excessive burn-off in the specimen containing the thermocouple holes which interfered with the heat flow. This belief is supported by the fact, illustrated in Fig. 5, that for thicker specimens (0.375 and 0.500 in.) where the blocking effect of the thermocouple holes was reduced, the discrepancy is no longer appreciable.

SAE 1020 Steel of Round Section

In Table 3 are presented the temperature distributions[3] in round SAE 1020 round steel bars of three diameters, 0.625, 0.750 and 1.000 in. Since the diameters of these specimens are large compared with the thickness of the rectangular sections, the correction factor is generally of smaller magnitude.

If the experimental nondimensional temperatures are plotted against the instantaneous distance from the interface ξ, as in Fig. 6, a large scattering results as a consequence of the wide variations in platen acceleration. In contrast to this, these temperatures, when corrected and plotted versus the equivalent distance η, agree remarkably well with the standard distribution curve (solid curve in Fig. 7), considering the magnitude of experimental errors in the temperature measurement.

Sintered Molybdenum of Round Section

The temperature distributions established during parabolic flashing of $^1/_2$-in. diam sintered molybdenum rounds[4] are given in Table 4. Since the thermal diffusivity of molybdenum varies considerably with temperature, the value $k = 0.00465$ in.^{2}/sec was selected from a preliminary calculation (Appendix C). The melting temperature T_m was taken as 4760° F.

The corrected nondimensional temperature is shown in Fig. 8 as a function of the instantaneous equivalent dis-

Table 2—Experimental Temperature Distributions in Parabolic Flashing of Rectangular Sectioned SAE 1020 Steel. From Data of Nippes, Savage, McCarthy and Smith, The Welding Journal, 30(12), Research Suppl., 585-s–601-s (1951)

Section size in.^{2}	Platen acceleration g_p in./sec.^{2}	Initial clamping distance $2(l_o)$ in.	$(\frac{g_p}{k^2})^{\frac{1}{3}}$ in.$^{-1}$	Experimental temperature distributions: ψ = Nondimensional temperature values; η = Nondimensional distance values; ξ = Instantaneous distance from flashing interface, in.	0.05	0.10	0.15	0.25	0.35	0.45	Correction factor r
	0.004	1.20	3.96	ψ	----	.794	.656	.400	.262	.180	0.109
				η	----	.366	.549	.915	1.281	1.646	
	0.010	1.20	4.98	ψ	.906	.737	.532	.344	.202	.135	0.039
				η	.249	.498	.748	1.246	1.745	2.242	
	0.020	1.20	6.26	ψ	.757	.569	.412	.266	.180	.116	0.014
				η	.313	.626	.939	1.452	2.188	2.814	
	0.004	1.50	3.66	ψ	----	.849	.700	.319	.288	.206	0.066
				η	----	.366	.549	.915	1.281	1.646	
	0.010	1.50	4.98	ψ	.944	.744	.576	.351	.225	.157	0.019
				η	.249	.498	.748	1.246	1.745	2.242	
	0.020	1.50	6.26	ψ	.775	.606	.438	.255	.183	.120	0.006
				η	.313	.626	.939	1.562	2.188	2.814	
1/4 x 2	0.040	1.50	7.90	ψ	.644	.494	.363	.225	.131	.082	0.002
				η	.395	.790	1.185	1.975	2.765	3.555	
	0.100	1.50	10.69	ψ	.496	.337	.269	.157	.120	.082	0.000
				η	.535	1.069	1.604	2.673	3.741	4.810	
	0.900	1.50	22.30	ψ	.363	.230	.160	.093	.071	----	0.000
				η	1.115	2.230	3.345	5.575	7.805	----	
	0.010	1.80	4.98	ψ	.944	.737	.550	.363	.243	.173	0.010
				η	.249	.498	.748	1.246	1.745	2.242	
	0.020	1.80	6.26	ψ	.812	.625	.457	.292	.191	.131	0.002
				η	.313	.626	.939	1.562	2.188	2.814	
	0.024	2.78	6.66	ψ	.906	.719	.494	.307	.195	.135	0.000
				η	.333	.666	.999	1.665	2.331	2.997	
	0.054	2.78	8.72	ψ	.726	.532	.389	.232	.150	.105	0.000
				η	.436	.872	1.308	2.180	3.052	3.924	
	0.004	1.20	3.66	ψ	.962	.775	.599	.382	.255	.183	0.071
				η	.183	.366	.549	.915	1.281	1.646	
	0.010	1.20	4.98	ψ	.812	.625	.457	.294	.154	.142	0.022
				η	.249	.498	.748	1.246	1.745	2.242	
3/8 x 2	0.005	1.80	3.66	ψ	----	.812	.663	.419	.314	.243	0.026
				η	----	.366	.549	.915	1.281	1.646	
	0.010	1.80	4.98	ψ	.944	.719	.532	.344	.243	.168	0.005
				η	.249	.498	.748	1.246	1.745	2.242	
	0.020	1.80	6.26	ψ	.757	.532	.363	.239	.165	.114	0.001
				η	.313	.626	.939	1.562	2.188	2.814	
	0.004	1.80	3.66	ψ	----	.775	.588	.400	.277	.210	0.017
				η	----	.366	.549	.915	1.281	1.646	
1/2 x 2	0.010	1.80	4.98	ψ	.812	.644	.457	.288	.206	.150	0.003
				η	.249	.498	.748	1.246	1.745	2.242	

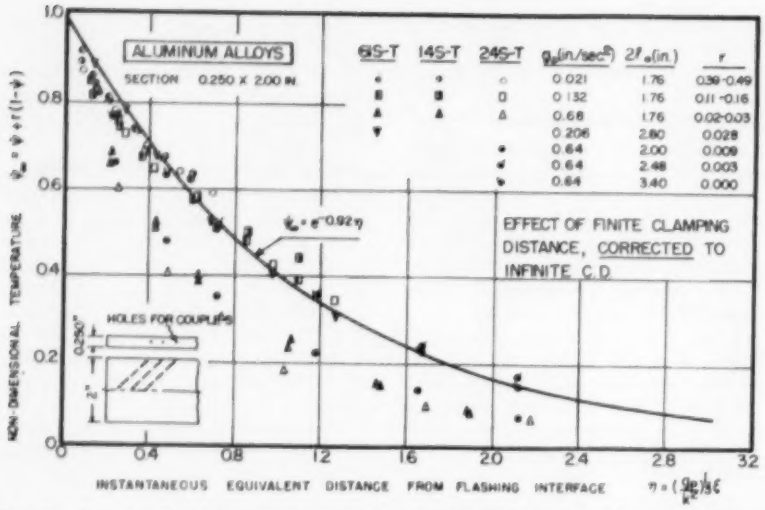

Fig. 3 Standardized temperature distribution in parabolic flashing of aluminum alloys

tance. It is seen that the experimental points lie consistently somewhat above the standard distribution curve. To account for this deviation, the experimental errors embodied in obtaining the temperature distribution data must be examined. Considering the much higher temperatures involved in the flashing of molybdenum than those in steel or aluminum alloys, an error larger than ±50° F is to be expected in the temperature measurement. Furthermore, since the initial-interface to thermocouple-hole distances were measured from radiographs of the $\frac{1}{2}$-in. specimens taken with the limited available voltage of 220 kvp,[4] the measured distances tended to be slightly too large owing to the insufficient contrast of the radiographs. It can be shown readily that an error of +0.05 in. in the measurement is enough to cause the discrepancy observed in Fig. 8.

7. Evaluation of Critical Burn-Off

In practical flashing, the temperature of the interface may vary locally due to the localized nature of the short circuits involved in the flashing action. Consequently, the interface temperature mentioned in this paper means the average value of the temperatures across the interface. Furthermore, since the flashing interface is defined as the solid surface from which the short circuits originate, the interface temperature can never exceed the melting temperature of the specimen.

The interface temperature will rise steadily with increasing burn-off until, at the critical burn-off, it reaches the melting point and remains unchanged thereafter. This stabilization of the

Table 3—Experimental Temperature Distributions in Parabolic Flashing of Round Sectioned SAE 1020 Steel. From Data of Nippes, Savage, Smith, Grotke and McCarthy, The Welding Journal, 32(3), Research Suppl., 113-s–122-s (1953)

Diameter of specimen in.	Platen acceleration g_p in./sec.^{2}	Initial clamping distance $2(l_o)$ in.	$(\frac{g_p}{k^2})^{\frac{1}{3}}$ in.$^{-1}$	Experimental temperature distributions: ψ = Nondimensional temperature values; η = Nondimensional distance values; ξ = Instantaneous distance from flashing interface, in.	0.05	0.10	0.15	0.25	0.35	0.45	Correction factor r
	0.0042	1.50	3.73	ψ	.831	.719	.569	.400	.273	.202	0.049
				η	.186	.373	.557	.931	1.303	1.670	
	0.0070	1.50	4.42	ψ	.831	.681	.543	.391	.255	.185	0.025
				η	.221	.442	.662	1.104	1.545	1.990	
5/8	0.0192	1.50	6.19	ψ	.784	.592	.446	.288	.225	.150	0.004
				η	.308	.619	.926	1.540	2.162	2.790	
	0.0042	1.80	3.73	ψ	.831	.719	.588	.400	.285	.187	0.030
				η	.186	.373	.557	.931	1.303	1.670	
	0.0192	1.80	6.19	ψ	.794	.606	.476	.326	.232	.165	0.002
				η	.308	.619	.926	1.540	2.162	2.790	
	0.0042	1.50	3.73	ψ	.831	.719	.606	.397	.288	.197	0.040
				η	.186	.373	.557	.931	1.303	1.670	
	0.0192	1.50	6.19	ψ	.728	.569	.419	.284	.189	.129	0.003
				η	.308	.619	.926	1.540	2.162	2.790	
3/4	0.0200	1.50	6.27	ψ	.757	.569	.400	.262	.180	.112	0.003
				η	.313	.627	.940	1.565	2.195	2.824	
	0.0340	1.50	7.48	ψ	.681	.476	.351	.225	.157	.101	0.001
				η	.373	.748	1.122	1.870	2.620	3.370	
	0.0750	1.50	9.73	ψ	.569	.363	.239	.139	.084	.052	0.000
				η	.485	.973	1.458	2.430	3.405	4.380	
	0.0042	1.50	3.73	ψ	.831	.719	.569	.382	.262	.210	0.026
				η	.186	.373	.557	.931	1.303	1.670	
	0.0070	1.50	4.42	ψ	.812	.644	.513	.363	.264	.187	0.012
				η	.221	.442	.662	1.104	1.545	1.990	
1	0.0120	1.50	5.29	ψ	.757	.588	.438	.277	.204	.139	0.004
				η	.264	.529	.793	1.320	1.850	2.380	
	0.0042	2.00	3.73	ψ	.831	.719	.569	.400	.299	.214	0.011
				η	.186	.373	.557	.931	1.303	1.670	
	0.0120	2.00	5.29	ψ	----	.606	.476	.326	.232	.172	0.001
				η	----	.529	.793	1.320	1.850	2.380	

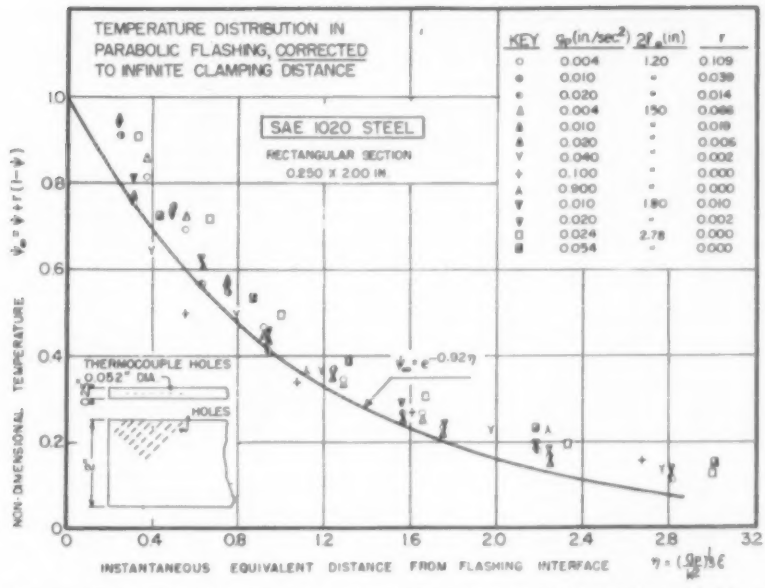

Fig. 4 Standardized temperature distribution in parabolic flashing of $^1/_4$- x 2-in. rectangular-sectioned SAE 1020 steel

interface temperature is an important requisite condition to establish the so-called "stabilized temperature distribution" during flashing. As shown experimentally,[2–4] the temperature at various distances from the interface does not approach the stabilized value asymptotically. Rather, the stable temperature at a particular point is reached immediately after the critical burn-off.

According to a simplified mathematical analysis of parabolic flashing (Appendix I), the theoretical critical burn-off per specimen B_c may be evaluated from the relation

$$\eta_{B_c} \equiv (g_p/k^2)^{1/3}B_c = 1.00 \quad (16)$$

where η_{B_c} is the equivalent critical burn-off. It is observed that with a higher acceleration or a lower thermal diffusivity, the temperature gradient is steeper, thus expediting the increase of the interface temperature with a smaller burn-off. Figure 9 shows the experimental values of the equivalent critical burn-off versus the initial clamping distance. Since eq 16 has been derived for an infinite clamping distance, the equivalent critical burn-off should be compared only with the experimental values obtained from long clamping distances. In this case, Fig. 9 indicates that the average equivalent critical burn-off of steel and aluminum alloys obeys the relation

$$(g_p/k^2)^{1/3}B_c = 0.70 \quad (16a)$$

The discrepancy between eqs 16 and 16*a* is to be expected since the Joule heating of the specimen by the flashing current has been neglected in the simplified mathematical analysis for reasons given in Appendix A. This additional heating will reduce the critical burn-off given by eq 16.

For linear flashing, not enough data are available to determine the experimental equivalent critical burn-off. The theoretical relation corresponding to eq 16 is given in Appendix I to be

$$B_c = 1.82(k/U_p) \quad (17)$$

If the Joule effect is assumed to be independent of the flashing pattern, the experimental critical burn-off for linear flashing may then be

$$B_c = 0.70 \times 1.82\,(k/U_p) = 1.27\,(k/U_p) \quad (17a)$$

Equation 16 for parabolic flashing can be replaced by

$$B_c = 2.00(k/V_p) \quad (18)$$

where V is the instantaneous platen velocity at the critical burn-off (Appendix H). Comparing eqs 17 and 18, it will be noticed that the critical burn-off is almost equal for parabolic and linear flashing provided that the same critical platen velocity is used in both cases. However, it is generally difficult in linear flashing to employ a platen velocity as large as is usually obtained at the critical burn-off in parabolic flashing, without the specimens sticking together at the very start of flashing. This handicap which necessitates the use of slow platen velocities in linear flashing, explains, in general, the well-established experimental observation that a stabilized temperature distribution which comes into existence early in parabolic flashing, has not been obtained in linear flashing except for long clamping distances. In most instances where the clamping distance is short and the platen velocity low, by the time the "would be" critical burn-off is approached, the flashing interface is so close to the edge of the clamping jaws that any tendency for the temperatures to be stabilized is disturbed.

8. Applications: Determination of Temperature Distribution and Flashing Conditions in Parabolic Flashing

Using the results of the present analysis, it is possible to predict, in the case of parabolic flashing, the temperature distribution prevailing in a given material under a given set of flashing conditions. Conversely, suitable conditions which will produce a desired temperature distribution may also be determined. These applications are illustrated by the following examples.

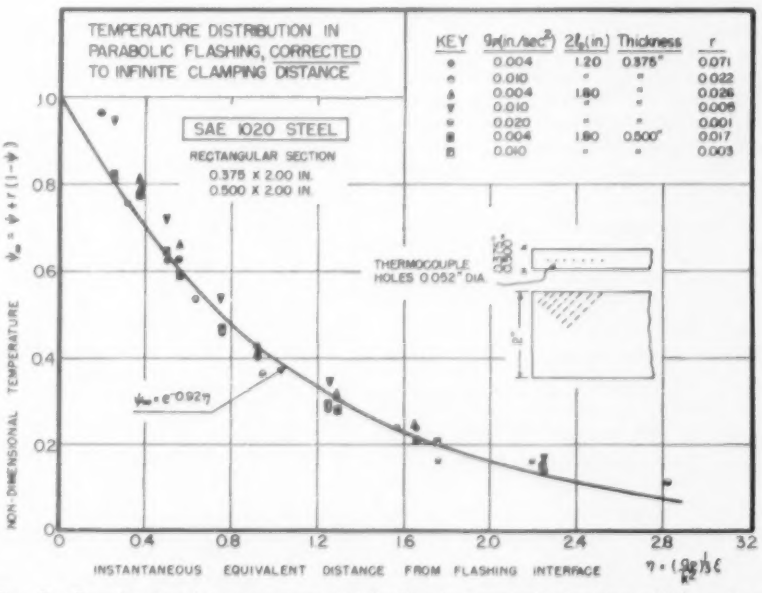
TEMPERATURE DISTRIBUTION IN PARABOLIC FLASHING, CORRECTED TO INFINITE CLAMPING DISTANCE
SAE 1020 STEEL
RECTANGULAR SECTION
0.375 X 2.00 IN.
0.500 X 2.00 IN.
KEY
THERMOCOUPLE HOLES 0.052" DIA
NON-DIMENSIONAL TEMPERATURE
INSTANTANEOUS EQUIVALENT DISTANCE FROM FLASHING INTERFACE

Fig. 5 Standardized temperature distribution in parabolic flashing of $^3/_8$- x 2-in. and $^1/_2$- x 2-in. rectangular-sectioned SAE 1020 steel

(1) Prediction of Temperature Distributions:

What will be the stabilized temperature distribution in two $^1/_4$- x 4-in. rectangular sections of 24S-T alloy (k^* = 0.078 in.^{2}/sec, T_m = 1057° F, T_0 = 80° F and ν = 1), when parabolically flashed with a platen acceleration of 0.64 in./sec^2 and an initial clamping distance of 2 in., with the $^1/_4$-in. dimension clamped between jaws?

(*A*) Determination of ψ_∞:

$$\eta \equiv (g_p/k^2)^{1/3}\xi = (0.64/0.078^2)^{1/3}\xi = 4.72\xi$$

$$\psi_\infty = e^{-0.92\eta} = e^{-0.92\times 4.72\xi} = e^{-4.35\xi}$$

(*B*) Determination of correction factor r:

$$\eta_1 \equiv (g_p/k^2)^{1/3}(l_0 + \nu D) - 0.70 = (0.64/0.078^2)^{1/3}(1 + {}^1/_4) - 0.70 = 5.2$$

$$r = e^{-0.92\eta_1} = e^{-4.8} = 0.008$$

(*C*) Determination of temperature distribution:

$$\psi_\infty = \psi + r(1 - \psi)$$

$$\psi = (\psi_\infty - r)/(1 - r) = (e^{-4.35\xi} - 0.008)/(1 - 0.008) = 1.01\,(e^{-4.35\xi} - 0.008)$$

from which the temperature at any plane ξ in. from the flashing interface can be computed. When ξ = 0.1 in., for instance,

$$\psi = 1.01\,(e^{-0.435} - 0.008) = 0.646$$

$$0.646 = (T - T_0)/(T_m - T_0) = (T - 80)/(1057 - 80)$$

$$T = 712° \text{ F}$$

(2) Determination of Suitable Flashing Conditions:

Assume that for satisfactory welding of two $^3/_4$-in. diam round sections of mild steel (k = 0.0090 in.^{2}/sec, T_m = 2750° F, T_0 = 80° F and ν = $^1/_2$) with a parabolic flashing pattern, a distance ξ = 0.15 in. from the flashing interface should be heated to temperatures higher than 1400° F. Determine a satisfactory platen acceleration g_p, a suitable initial clamping distance $2(l_0)$, and the minimum burn-off per specimen B_r before upsetting.

(*A*) Determination of ψ corresponding to T:

$$\psi \equiv (T - T_0)/(T_m - T_0) = (1400 - 80)/(2750 - 80) = 0.494$$

(*B*) Determination of maximum g_p (from eq 15, §5):

$$e^{-0.92G\xi} - \psi = 1.904(1 - \psi)e^{-0.92GL} \quad (15)$$

where $G \equiv (g_p/k^2)^{1/3}$ and $L \equiv l_0 + \nu D$, it follows that for the present case,

$$e^{-0.138G} - 0.494 = 0.963e^{-0.92GL} \quad (15a)$$

Since the right-hand side of eq 15*a* cannot be negative,

$$e^{-0.138G} - 0.494 \geq 0$$

or

$$G = (g_p/k^2)^{1/3} = (g_p/0.0090^2)^{1/3} \leq 5.10 \text{ in.}^{-1}$$

Hence $g_{p,\max}$ = 0.0107 in./sec^2 corresponding to which, $2(l_0) = \infty$ and r = 0. For accelerations greater than 0.0107 in./sec^2, the temperature gradient will be so steep that the temperature at the plane 0.15 in. from the flashing interface cannot rise to 1400° F.

(*C*) Determination of combinations of flashing conditions:

In practice where the clamping distance is always finite, platen accelerations smaller than $g_{p,\max}$ must be used. For the present case, by choosing a value of g_p, the corresponding value of $2(l_0)$ necessary to give the required temperature distribution can be computed from eq 15*a*. The results so obtained are given in Table 5 from which a graph of either L versus G or, as is done in Fig. 10, $2(l_0)$ versus g_p, may be

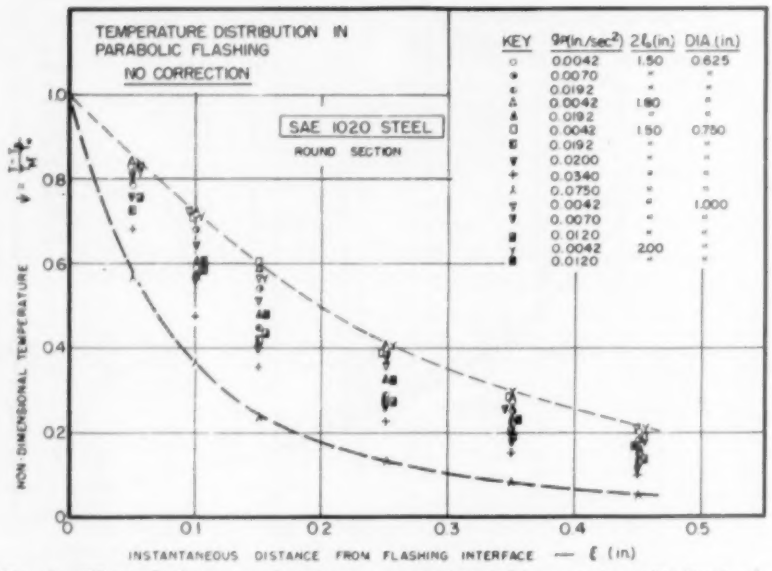
TEMPERATURE DISTRIBUTION IN PARABOLIC FLASHING
NO CORRECTION
SAE 1020 STEEL
ROUND SECTION
KEY | gp(in./sec²) | 2l₀(in.) | DIA (in.)
○ 0.0042 1.50 0.625
● 0.0070 " "
◐ 0.0192 " "
△ 0.0042 1.80 "
▲ 0.0192 " "
□ 0.0042 1.50 0.750
■ 0.0192 " "
▽ 0.0200 " "
+ 0.0340 " "
λ 0.0750 " "
▽ 0.0042 " 1.000
▼ 0.0070 " "
▣ 0.0120 " "
▽ 0.0042 2.00 "
▣ 0.0120 " "
NON DIMENSIONAL TEMPERATURE
INSTANTANEOUS DISTANCE FROM FLASHING INTERFACE — ξ (in)

Fig. 6 Effect of platen accelerations on experimental temperature distribution in parabolic flashing of round-sectioned SAE 1020 steel

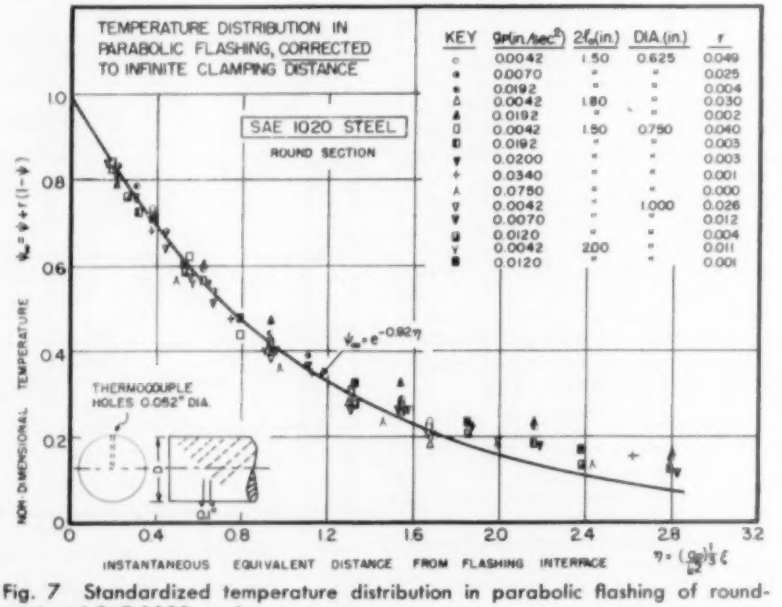
TEMPERATURE DISTRIBUTION IN PARABOLIC FLASHING, CORRECTED TO INFINITE CLAMPING DISTANCE
SAE 1020 STEEL
ROUND SECTION
KEY | gp(in./sec²) | 2l₀(in.) | DIA (in.) | r
○ 0.0042 1.50 0.625 0.049
● 0.0070 " " 0.025
◐ 0.0192 " " 0.004
△ 0.0042 1.80 " 0.030
▲ 0.0192 " " 0.002
□ 0.0042 1.50 0.750 0.040
■ 0.0192 " " 0.003
▽ 0.0200 " " 0.003
+ 0.0340 " " 0.001
λ 0.0750 " " 0.000
▽ 0.0042 " 1.000 0.026
▼ 0.0070 " " 0.012
▣ 0.0120 " " 0.004
▽ 0.0042 2.00 " 0.011
▣ 0.0120 " " 0.001
ψ∞ = e^-0.92η
THERMOCOUPLE HOLES 0.052" DIA.
NON DIMENSIONAL TEMPERATURE
INSTANTANEOUS EQUIVALENT DISTANCE FROM FLASHING INTERFACE

Fig. 7 Standardized temperature distribution in parabolic flashing of round-sectioned SAE 1020 steel

* For a material not discussed in this paper, its average value of thermal diffusivity k_m can be determined according to the method given in Appendix C.

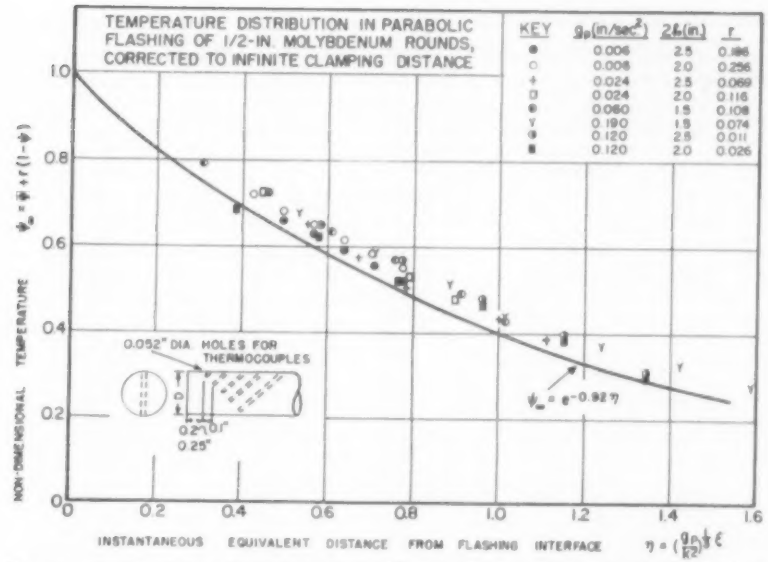

Fig. 8 Standardized temperature distribution in parabolic flashing of 1/2-in. diam sintered molybdenum rods

plotted. It is seen that the graph in Fig. 10 is divided into two regions marked, respectively, practicable and impracticable. In the practicable range, a reduction in g_p requires a decrease in $2(l_0)$ in order to maintain the given temperature distribution. Such related changes in flashing conditions, in essence, reflect a balance between the gentle temperature gradient (and hence a high temperature at a given distance from the interface) associated with slow platen accelerations and the enhanced rate of heat dissipation associated with short clamping distances. However, the curve in Fig. 10 passes through a minimum at about $g_p = 0.0022$ in./sec². For accelerations below this value, increasingly longer clamping distances are required. Analytically, this relationship merely indicates that at such low platen accelerations, the rate of heat input is so low that the temperature at $\xi = 0.15$ in. cannot reach 1400° F unless the rate of heat dissipation is reduced by lengthening the clamping distance. Practically, it is very unlikely that the steel can be flashed at such low accelerations.

It is thus evident that in order to produce the required temperature distribution, any combination of platen acceleration and initial clamping distance chosen from the practicable range of Fig. 10 may be used.

(*D*) Determination of critical burn-off B_c:

Once the desired g_p is chosen, the minimum burn-off in inch per specimen before upsetting can be determined from $B_c = 0.70/G$.

9. Conclusions

As a result of the mathematical analysis of the flashing operation, the following conclusions have been obtained:

1. For a general flashing pattern in which the platen displacement Xp varies with time t according to $X_p = at^n$, where a and n are arbitrary constants, the variables which affect the temperature distribution, can be grouped into a single nondimensional parameter P, designated as the instantaneous-equivalent distance, by

$$P = (na/k^n)^{1/(2n-1)}\,\xi$$

where k is the thermal diffusivity of the specimen and ξ the instantaneous distance from the flashing interface to the point in question.

2. The nondimensional stabilized temperature resulting from a given flashing pattern with an infinite clamping distance, is a function of the nondimensional parameter P, or $\psi_\infty = f(P)$. This functional relationship is universal for all materials and all flashing conditions.

3. In general, the standard form of stabilized temperature distribution is approximated closely by an inverse exponential function, $\psi_\infty = e^{-\alpha P}$, where α is a universal constant, the value of which depends only on the flashing pattern. In linear flashing where $n = 1$ and $P = \lambda = (U_p/k)\xi$, the theoretical relationship appears to be $\psi_\infty = e^{-0.77\lambda}$. For parabolic flashing where $n = 2$ and $P = \eta = (g_p/k^2)^{1/3}\xi$, ($g_p$ is the platen acceleration), the theoretical and experimental standard distribution forms are, respectively,

$$\psi_\infty = e^{-1.5\eta}$$

and

$$\psi_\infty = e^{-0.92\eta}$$

The latter relationship has been verified by extensive experimental results for aluminum alloys, SAE 1020 steel and molybdenum.

4. For finite clamping distance, equations have been presented to estimate the cooling effect of clamping jaws on the stabilized temperature distribution. For parabolic flashing, the corrected nondimensional temperature ψ_∞ is related to the experimental nondimensional temperature ψ by

$$\psi_\infty = \psi + r(1-\psi)$$
$$r = e^{-0.92\eta_1}$$

and

$$\eta_1 = (g_p/k^2)^{1/3}(l_0 + D') - 0.70$$

where l_0 is one-half of the initial clamping distance, and D' is equal to the thickness of a rectangular-sectioned specimen, or to one-half the diameter of a round specimen. The cooling effect is thus more pronounced for lower ac-

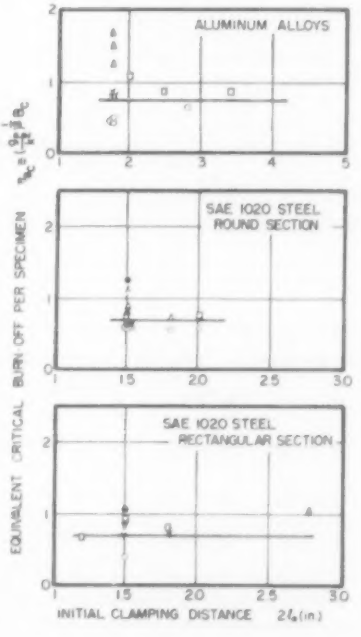
ALUMINUM ALLOYS
SAE 1020 STEEL ROUND SECTION
SAE 1020 STEEL RECTANGULAR SECTION
EQUIVALENT CRITICAL BURN OFF PER SPECIMEN $B_c = \frac{g_p}{k^2}\beta_c$
INITIAL CLAMPING DISTANCE 2l_0(in.)

Fig. 9 Effect of initial clamping distance on experimental equivalent critical burn-off in parabolic flashing

Table 4—Experimental Temperature Distributions in Parabolic Flashing of $^1/_2$-in. Diam Molybdenum Rounds. From Data of Nippes and Chang, The Welding Journal, 34(3), Research Suppl., 132-s to 140-s (1955)

Platen acceleration g_p in./sec^2	Initial clamping distance $2(l_o)$ in.	$(\frac{g_p}{k^2})^{\frac{1}{3}}$ in.$^{-1}$		Experimental temperature distributions: ψ = Nondimensional temperature values, η = Nondimensional distance values, ξ = Instantaneous distance from flashing interface, in. 0.10	0.15	0.20	0.25	0.30	0.35	0.40	0.45	0.50	Correction factor r
0.006	2.5	1.41	ψ	----	----	----	----	----	.580	.538	.495	.453	0.186
			η	----	----	----	----	----	.494	.564	.635	.705	
0.006	2.0	1.41	ψ	----	----	----	----	.625	.570	.528	.485	.442	0.256
			η	----	----	----	----	.423	.494	.564	.635	.705	
0.024	2.5	2.23	ψ	----	----	----	.625	.538	.475	.430	.390	.335	0.069
			η	----	----	----	.558	.670	.780	.892	1.000	1.110	
0.024	2.0	2.23	ψ	----	----	.688	.603	.528	.463	.410	----	----	0.116
			η	----	----	.446	.558	.670	.780	.892	----	----	
0.060	1.5	3.03	ψ	.752	.688	.591	.516	.430	.357	----	----	----	0.108
			η	.303	.455	.606	.758	.910	1.060	----	----	----	
0.090	1.5	3.54	ψ	.688	----	.550	.463	.390	.314	.270	.218	----	0.074
			η	.354	----	.707	.884	1.060	1.240	1.420	1.590	----	
0.120	2.5	3.82	ψ	----	.645	.560	.475	.390	.305	----	----	----	0.011
			η	----	.573	.763	.955	1.150	1.340	----	----	----	
0.120	2.0	3.82	ψ	.690	.615	.505	.453	.378	.282	----	----	----	0.026
			η	.382	.573	.763	.955	1.150	1.340	----	----	----	

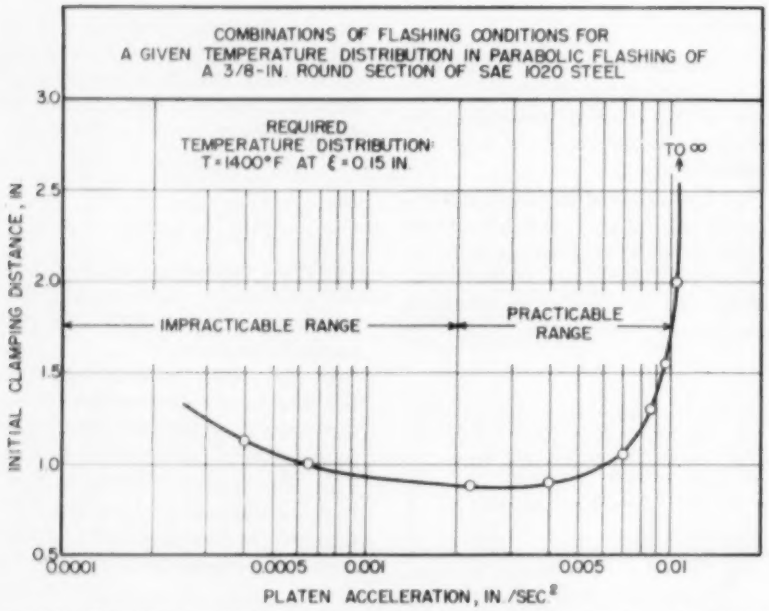

Fig. 10 Combinations of flashing conditions for a given temperature distribution in parabolic flashing of a given material

celeration, higher thermal diffusivity, shorter clamping distance or thinner specimens.

5. The critical burn-off per specimen necessary to establish a stabilized temperature distribution, can be predicted from

$$B_c = 0.70(g_p/k^2)^{-1/3}$$

for parabolic flashing. In connection with this, the practical experience that longer critical burn-off is required in linear than in parabolic flashing is explained.

6. Examples of predicting temperature distributions and predetermining flashing conditions have been given.

7. A reasonable method is suggested for determining an adequate average value of thermal diffusivity (Appendix C).

8. The effect of heat of transformation on the temperature distribution in steel is evaluated and found to be negligible (Appendix D).

Bibliography

1. Curran, R. M., Patriarca, P., and Hess, W. F., "Optimum Flash Welding Conditions, the

Table 5—Combinations of Flashing Conditions for Given Temperature Distribution

$G=(g_p/k^2)^{1/3}$	5.10	5.05	4.95	4.75	4.42	3.68	3.00	2.00	1.71
g_p, in./sec^2	0.0107	0.0104	0.0098	0.0087	0.0070	0.0040	0.0022	0.00065	0.00040
$L=l_o+\nu D$, in.	∞	1.177	0.942	0.834	0.734	0.650	0.634	0.700	0.755
$2(l_o)$, in.	∞	1.987	1.508	1.292	1.092	0.924	0.892	1.024	1.130
r	0	0.0080	0.0259	0.050	0.097	0.211	0.330	0.525	0.585

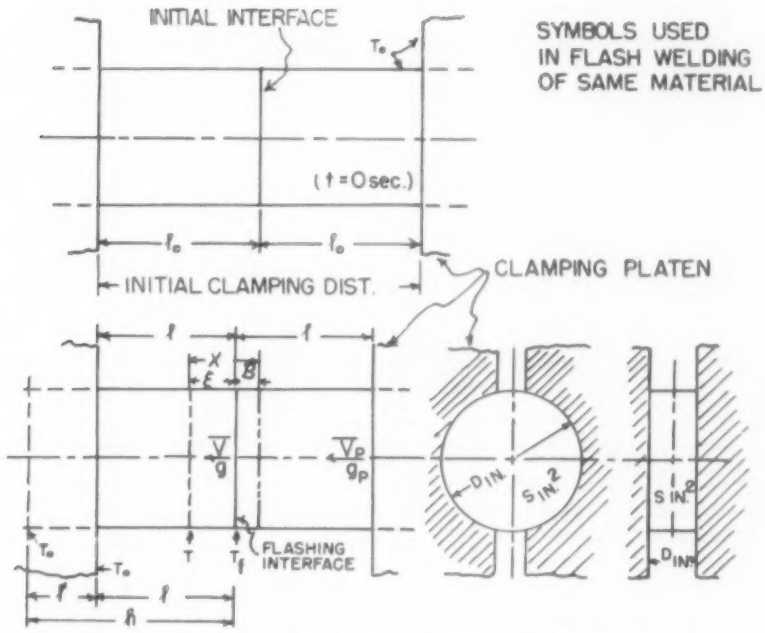

Fig. 11 A sketch illustrating the symbols used in the present analysis

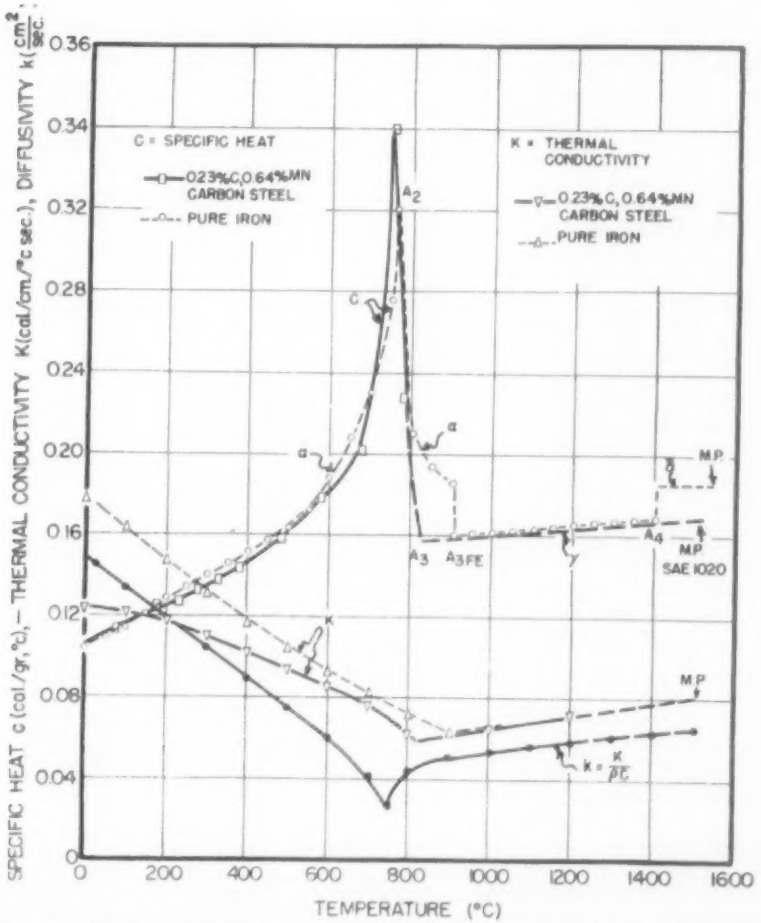

Fig. 12 Effects of temperature on specific heat, thermal conductivity and diffusivity of iron and mild steel

Importance of Temperature Measurements," The Welding Journal, **28** (12), Research Suppl., 577-s to 592-s (1948).

2. Nippes, E. F., Savage, W. F., McCarthy, J. J., and Smith, S. S., "Temperature Distribution During Flash Welding of Steel," *Ibid.*, **30** (12), Research Suppl., 585-s to 601-s (1951).

3. Nippes, E. F., Savage, W. F., Smith, S. S., McCarthy, J. J., and Grotke, G., "Temperature Distribution During Flash Welding of Steel—Part II," *Ibid.*, **32** (3), Research Suppl., 113-s to 122-s (1953).

4. Nippes, E. F., and Chang, Wen H., "The Flash Welding of Commercial Molybdenum," *Ibid.*, **34** (3), Research Suppl., 132-s to 140-s (1955).

5. Rosenthal, D., "Mathematical Theory of Heat Distribution During Welding and Cutting," *Ibid.*, **20** (5), Research Suppl., 220-s to 234-s (1941).

6. Carslaw, H. S., and Jaeger, J. C., *Conduction of Heat in Solids*, Clarendon Press, §106 (1948).

APPENDIX

A. General Differential Equation of Temperature Distribution

The present mathematical analysis is based on a semi-infinite solid specimen being flashed at one end surface with the other end kept at initial temperature T_0. The Joule effect* is neglected and the flashing interface is considered the only heat source. Also, the temperature distribution is assumed unidirectional along the longitudinal axis of specimen by neglecting the small heat loss into surroundings and assuming a uniform flashing across the interface.

With respect to a fixed coordinate system (x, y, z), the general differential equation of temperature distribution in which the thermal conductivity K of the solid is considered temperature-dependent is

$$\frac{\partial T}{\partial t} = \frac{1}{\rho c}\frac{\partial}{\partial x}\left(K\frac{\partial T}{\partial x}\right) \qquad (19)$$

where T, t, x, ρ and c represent, respectively, temperature, time, distance, density of solid and specific heat of solid. When K is assumed constant, the equation reduces to

$$\frac{\partial T}{\partial t} = k\frac{\partial^2 T}{\partial x^2} \qquad (20)$$

where k is the thermal diffusivity.

With respect to a coordinate system (ξ, y, z), which is moving with the interface, the relation $\xi = x - B(t)$, where $B(t)$ is the burn-off per specimen (Fig. 11), enables eq 19 to be transformed into

$$\frac{\partial}{\partial \xi}\left(K\frac{\partial \theta}{\partial \xi}\right) + \rho c V\frac{\partial \theta}{\partial \xi} = \rho c\frac{\partial \theta}{\partial t} \qquad (21)$$

where $\theta \equiv T - T_0$. The boundary conditions are

$$\text{at } \xi = 0 \text{ and } t = t: \quad \theta = \theta_i(t)$$
$$\text{at } \xi = \infty \text{ and } t = t: \quad \theta = 0$$

where $\theta_i \equiv (T_i - T_0)$ is the average temperature rise of the flashing interface.

B. Steady State Temperature Distribution—General Solution

In a steady state, $\partial\theta/\partial t = 0$ and eq

* Due to its transient and intermittent nature, the flashing current is difficult to measure. In addition, the resistance of the specimen is continuously changing during the flashing cycle. An evaluation of the Joule heating at the present time has not been successful.

21 becomes

$$\frac{d}{d\xi}\left(K\frac{d\theta}{d\xi}\right) + \rho c U\frac{d\theta}{d\xi} = 0 \quad (22)$$

where a constant velocity U replaces the general velocity V, since eq 22 obviously cannot contain a time-dependent variable.

Integrating with respect to ξ from ξ to ∞, and recalling $(d\theta/d\xi)_\infty = 0$, eq 22 becomes

$$\int_\theta^{\theta_i} \frac{K\ d\theta}{\int_0^\theta \rho c\ d\theta} = U\xi \quad (23)$$

which can be evaluated graphically if K and ρc are given as functions of temperature.

For constant K, eq 23 reduces to

$$\int_\theta^{\theta_i} \frac{d\theta}{\int_0^\theta (d\theta/k)} = U\xi$$

which, for constant thermal diffusivity k, leads to

$$\theta/\theta_i = e^{-(U/k_m)\xi} \quad (24)$$

Equation 24 is in agreement with Rosenthal's result[5] which was once used by Rensselaer Polytechnic Institute investigators[1] in a study of flashing of aluminum alloys.

C. Determination of Average Value of Thermal Diffusivity

The most general differential equation containing K, ρ and c as functions of temperature is too complex to allow an easy solution. For the present purpose, the thermal diffusivity, $k = K/\rho c$, is assumed to be a constant over the necessary temperature range. Since k usually shows an irregular variation with temperature, especially in steels, a method for determining an adequate mean value of k merits consideration.

Solving eq 23 numerically, an exact curve of temperature vs. $U\xi$ is obtained in which k is treated as temperature-dependent. If instead, the thermal diffusivity is given a constant value of, say, k_m, another curve can be drawn from eq 24. The agreement between this curve and the exact curve will depend on the value of k_m. By using different values of k_m, therefore, a series of curves are produced from which the particular value of k_m corresponding to the curve which best matches the exact curve, can be chosen as the adequate mean value of k.

SAE 1020 Steel

Figure 12 illustrates the variation of the thermal constants of SAE 1020 steel with temperature, the data being taken from *ASM Metals Handbook*. Values of the thermal conductivity between 1200° C and the melting point 1510° C (taken as the mean value of solidus and liquidus) were extrapolated from those at lower temperatures, whereas values of the specific heat above 800° C have been estimated from those of pure iron, also given in the *Handbook*. The density ρ has been calculated from

$$\rho = \rho_0[1 - 3\alpha(T - T_0)]$$

where

$\rho_0 = 7.86$ gm/cm³,
$\alpha = 1.48 \times 10^{-5}$/°C,
$T_0 = 80°$ F.

The coefficient of linear expansion is taken as equal to the mean value for the temperature range from 20 to 700° C.

The exact curve, calculated from the data given in Fig. 12, and three approximate curves are shown in Fig. 13. By inspection the best approximation of the exact curve is given by $k_m = 0.0090$ in.²/sec, which, compared with other k_m values, gives the least maximum deviation (about 70° F) for temperatures higher than 900° F. Since only the higher temperature range is of importance in flashing, the average value of thermal diffusivity for SAE 1020 steel is taken as 0.0090 in.²/sec.

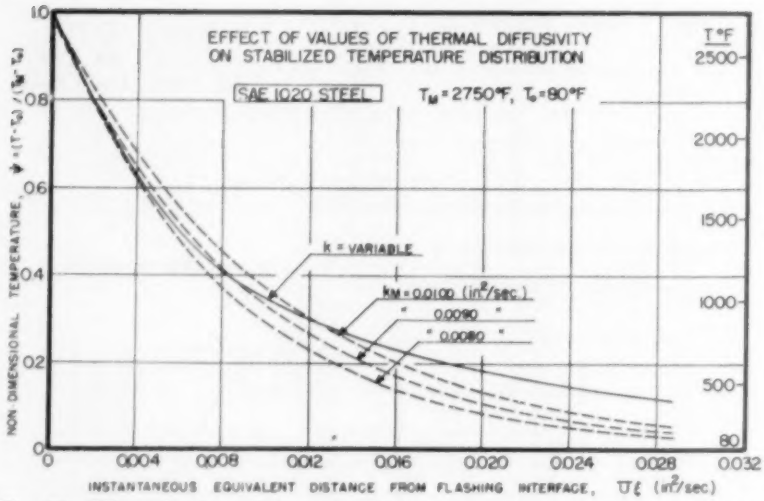

Fig. 13 Effect of values of thermal diffusivity on stabilized temperature distribution in SAE 1020 steel

Aluminum Alloys

The thermal diffusivity of aluminum alloys does not change appreciably with temperature. Accordingly, its value at 200° C has been used for each alloy for the range from room temperature to the melting point. These have been kindly supplied by C. S. Taylor, Aluminum Research Laboratories, Aluminum Company of America, as follows:

Alloy	*Thermal diffusivity at 200° C, in.²/sec*
14S-T	0.096
24S-T	0.078
61S-T	0.098

Molybdenum

The thermal diffusivity of molybdenum shows considerable variation with temperature, although not as much as for steel. Using the same procedure as described above, the most adequate value of average thermal diffusivity is taken as $k_m = 0.0465$ in.²/sec.

D. Effect of Heat of Transformation in Steel

The heat of transformation from gamma to alpha iron is about 5 cal/g, which if utilized completely by the transformed volume, may change the temperature of pure iron by as much as 57° F. Actually, much of this heat is dissipated away. For simplicity, the heat of transformation of SAE 1020 steel is assumed to be equal to that of pure iron. For a constant thermal diffusivity, eq 22 becomes

$$\frac{d^2\theta}{d\xi^2} = \frac{U}{k}\frac{d\theta}{d\xi} \quad (25)$$

The condition of transformation at A_3 = 1520° F is

$$-\left(\frac{d\theta}{d\xi}\right)_1 = -\left(\frac{d\theta}{d\xi}\right)_2 + \frac{q_t}{c}\frac{U}{k} \quad (26)$$

where q_t is the heat of transformation, 5 cal/g. The subscripts 1 and 2 refer, respectively, to the portions of specimen which are higher and lower in temperature than the transformation temperature.

Solving eq 25, the temperature distributions are

$$T - (T_0 - q_t/c) = [T_i - (T_0 - q_t/c)]e^{-(U/k)\xi} \quad (27a)$$

for $T_i \geq T \geq T_3$ or $0 \leq \xi \leq \xi_3$; and

$$T - T_0 = [(T_3 - T_0)\ e^{(U/k)\xi_3}]e^{-(U/k)\xi} \quad (27b)$$

for $T_3 \geq T \geq T_0$ or $\xi_3 \leq \xi \leq \infty$, where $e^{-(U/k)\xi_3} = (T_3 - T_0 + q_t/c)/(T_i - T_0 + q_t/c)$

When $q_t = 0$, both distributions reduce to the simple relation

$$T - T_0 = (T_i - T_0)e^{-(U/k)\xi} \quad (28)$$

For SAE 1020 steel, $T_i = T_m = 2750°$ F, $T_3 = A_3 = 1520°$ F, $T_0 = 80°$ F, $q_t/c = 57°$ F, and $k = 0.0090$

in.²/sec, it is found by comparing eqs 27 and 28 that the thermal effect of gamma to alpha iron transformation is to cool the specimen slightly. The maximum cooling is approximately 27° F at the transforming section, which is less than the experimental error in the temperature measurement.

E. Mathematical Analysis of Heat Flow in Flashing

In solving eq 20, the heat-source method[6] is preferred. The heat liberated by the electric current at the interface will be dissipated by conduction into the pair of specimens. Although the instantaneous amount of heat dissipation may show many intermittent peaks within a fraction of one cycle (1/60 sec) of the flashing voltage, the average amount of heat dissipation can be considered as a continuous function of time so far as the macroscopic variation of temperature is concerned.

At time t' ($0 < t' < t$), the burn-off per specimen is $B(t')$, and the cross section at an instantaneous distance ξ from the flashing interface at time t, must be located at a distance ξ' at time t'. Hence,

$$\xi' = \xi + B(t) - B(t')$$

If the magnitude of heat dissipation at time t' is $q(t')$, then in an incremental time dt', a heat quantity of $q(t') \times dt'$ is dissipated into the pair of specimens. This heat will cause a temperature rise of $d\theta$ after a time elapse of $t - t'$, in the section located at an instantaneous distance of ξ from the flashing interface. According to the heat-source theory,[6] $d\theta$ for a moving heat source is given by

$$d\theta = \frac{q(t')\,dt'}{2\rho c[\pi k(t-t')]^{1/2}} e^{\frac{-(\xi')^2}{4k(t-t')}} \quad (29)$$

The temperature rise, $\theta \equiv T - T_0$, at the pertinent section in the specimen at time t, is thus obtained by integrating eq 29 with respect to t', from $t' = 0$ to $t' = t$, i.e.,

$$\theta = \frac{1}{2\rho c(\pi k)^{1/2}} \int_0^t e^{\frac{-(\xi + B - B')^2}{4k(t-t')}} \frac{q(t')}{(t-t')^{1/2}}\,dt' \quad (30)$$

where

$B - B' = B(t) - B(t')$.
$B - B' = U(t - t')$ for linear flashing.
$B - B' = (g/2)(t^2 - t'^2)$ for parabolic flashing.

It should be noted that the solution represented by eq 30 can be shown to satisfy the following equation which is modified from the general equation (eq 20) for flashing:

$$k\frac{\partial^2\theta}{\partial\xi^2} + V\frac{\partial\theta}{\partial\xi} = \frac{\partial\theta}{\partial t} \quad (31)$$

Moreover, eq 30 also satisfies the boundary conditions

at the interface: $q = q(t')$
at $\xi = \infty$: $\theta = 0$

Consequently, eq 30 is considered to be the required temperature distribution due to flashing.

The numerical computation of eq 31 requires either a definite form of $q(t')$ or a knowledge of the interface-temperature rise with time. Since the flashing phenomena are so complicated, a purely theoretical deduction of the functional form of $q(t')$ is a matter of extreme difficulty. If, however, it is assumed that the time rate of electrical heat input is proportional to the burn-off rate (flashing velocity), and the heat dissipation into specimens proportional to the electrical heat input, then as a first approximation,

$$q(t') = mV \quad (32)$$

where m is a proportionality constant. The validity of this approximation as well as the nature of m will be discussed in the following sections.

F. Rate of Heat Dissipation in Flashing

Integrating both sides of eq 31 with respect to ξ from 0 to ∞ gives

$$-K\left(\frac{\partial\theta}{\partial\xi}\right)_{\xi=0} = \rho c\theta_i V + \rho c\frac{\partial}{\partial t}\int_0^\infty \theta\,d\xi$$

in which $\theta_i \equiv (T_i - T_0)$ is the interface-temperature rise and $-K(\partial\theta/\partial\xi)_{\xi=0}$ the rate of heat flow per unit time per unit area across the interface. Since the heat is dissipated into both specimens, it follows that

$$q = 2\left[-K\left(\frac{\partial\theta}{\partial\xi}\right)_{\xi=0}\right] = 2\rho c\theta_i V + 2\frac{\partial Q}{\partial t} \quad (33)$$

where $Q \equiv (\rho c\int_0^\infty \theta\,d\xi)$ is the increase in heat content per unit area in one specimen at time t. When the burn-off approaches the critical value, $T_i \to T_m$ and $\partial Q/\partial t \to 0$. Equation 33 may then be written

$$q = (2\theta_m\rho c)V = [2(T_m - T_0)\rho c]V \quad (34)$$

By comparing eqs 32 and 34, the proportionality constant m is found to be

$$m = 2\theta_m\rho c \quad (35)$$

G. Nondimensional Temperature Distribution During Flashing

General Flashing Pattern

For a general flashing pattern, the platen movement is described by $X_p = at^n$ where a and n are arbitrary constants. Hence,

$$B - B' = a(t^n - t'^n)/2$$

and eq 34 becomes

$$q(t') = 2[(T_m - T_s)\rho c]V_i' = 2[(T_m - T_0)\rho c]\,(V_p'/2) = n\rho ca(T_m - T_0)\,(t')^{n-1}$$

where V_i' and V_p' are, respectively, instantaneous interface and platen velocities at time t'. Substituting into eq 30 and letting $\lambda \equiv (t - t')^{1/2}$, the following equation results after rearrangement:

$$\psi = 4/\sqrt{\pi}\int_0^{\sqrt{t}} (1 - \lambda^2/t)^{n-1} (e^{-(\xi + B - B')^2/4k\lambda^2})\,(nat^{n-1}/4\sqrt{k})d\lambda$$

Let

$$Y \equiv (V_p/4)(t/k)^{1/2} = (nat^{n-1}/4)(t/k)^{1/2}$$
$$A \equiv (V_p/8k)\xi = (nat^{n-1}/8k)\xi$$

and

$$\zeta \equiv (V_p/4\sqrt{k})\lambda = (nat^{n-1}/4\sqrt{k})\lambda$$

then

$$\lambda^2/t = \zeta^2/Y^2, \qquad \xi/2\sqrt{k}\lambda = A/\zeta, \qquad at^n/4\sqrt{k}\lambda = Y^2/n\zeta$$

and

$$a(t')^n/4\sqrt{k}\lambda = (Y^2/n\zeta)\,(1 - \zeta^2/Y^2)^n$$

From these relationships, the general temperature distribution equation is transformed into

$$\psi = 4/\sqrt{\pi}\int_0^Y \left(1 - \frac{\zeta^2}{Y^2}\right)^{n-1} e^{-\left\{\frac{A}{\zeta} + \frac{1}{n}\frac{Y}{\zeta}\left[1 - \left(1 - \frac{\zeta^2}{Y^2}\right)^n\right]\right\}^2} d\zeta \quad (36)$$

The equations for individual flashing patterns can be obtained readily from eq 36.

(a) Linear Flashing

In this case, $X_p = U_p t$, or $a = U_p$ and $n = 1$. Equation 36 reduces to

$$\psi \equiv \frac{T - T_0}{T_m - T_0} = \frac{4}{\sqrt{\pi}}\int_0^Y e^{-\left(\zeta + \frac{A}{\zeta}\right)^2} d\zeta \quad (37)$$

where

$A \equiv U_p\xi/8k$, $Y \equiv (U_p/4)(t/k)^{1/2}$ and $\zeta \equiv (U_p/4)\,[(t - t')/k]^{1/2}$

Since

$$\int_0^\infty e^{-(\zeta + A/\zeta)^2}\,d\zeta = \sqrt{\pi}\,e^{-4A/2}$$

the limiting value of ψ at $t = \infty$ is

$$\lim_{t\to\infty}\psi = 2e^{-U_p\xi/2k} = 2e^{-U\xi/k}$$

which gives a value of 2 at the flashing interface. Physically, $T_i \leq T_m$ and $\psi_i \leq 1$, it is therefore concluded that the critical burn-off is reached long before the mathematical limiting value of ψ.

(b) Parabolic Flashing

For parabolic flashing, $X_p = g_p t^2/2$, or $a = g_p/2 = g$ and $n = 2$. Hence,

$$\psi \equiv \frac{T - T_0}{T_m - T_0} = \frac{4}{\sqrt{\pi}}\int_0^Y (1 - \zeta^2/Y^2)\,e^{-\left(\zeta + \frac{A}{\zeta} - \frac{\zeta^3}{2Y^2}\right)^2} d\zeta \quad (38)$$

where

$A \equiv g_p t\xi/8k$
$Y \equiv (g_p t/4)(t/k)^{1/2}$
$\zeta \equiv (g_p t/4)\,[(t - t')/k]^{1/2}$

The interface temperature also approaches a limiting value of 2 at $t = \infty$.

(c) Cubic Flashing

For a cubic flashing pattern, the platen movement is given by $X_p = at^3$ and the temperature distribution equation is

$$\psi \equiv \frac{T - T_0}{T_m - T_0} = \frac{4}{\sqrt{\pi}} \int_0^Y (1 - \xi^2/Y^2)^2 e^{-\left\{\frac{A}{\xi} + \frac{Y^2}{3\xi}\left[1 - \left(1 - \frac{\xi^2}{Y^2}\right)^3\right]\right\}^2} d\xi \quad (39)$$

where

$A \equiv V_p\xi/8k$
$Y \equiv (V_p/4)\,(t/k)^{1/2}$
$\xi \equiv (V_p/4)\,[(t - t')/k]^{1/2}$

H. Parameters in the Flashing Operation

(a) Linear Flashing

Since from §2, $\lambda \equiv U_p\xi/k$ and $\lambda_B \equiv U_pB/k$, where λ_B is related to burn-off and λ to instantaneous distance from the flashing interface, it follows that

$$A = \lambda/8 \quad (40a)$$

and

$$Y = (2\lambda_B)^{1/2}/4 \quad (40b)$$

(b) Parabolic Flashing

Since $\eta \equiv (g_p/k^2)^{1/3}\xi$ and $\eta_B \equiv (g_p/k^2)^{1/3}B$, the following relationships exist:

$$A = \eta(\eta_B)^{1/2}/4 \quad (41a)$$
$$Y = (\eta_B)^{3/2}/\sqrt{2} \quad (41b)$$

and

$$\eta = (\eta_B)^{-1/2}(V_p\xi/2k) \quad (41c)$$

Thus, the temperature distribution (eq 38) is a function of η_B and η, in accordance with the prediction made in the dimensional analysis in §2. At the critical burn-off of $\eta_B = 1$, there exists the important relation

$$(g_p/k^2)^{1/3} = V_p/2k \quad (42)$$

(c) Cubic Flashing

From eq 6, §2, the parameters for cubic flashing are found to be

$$\omega \equiv (3a/k^2)^{1/3}\xi \quad \text{and} \quad \omega_B \equiv (3a/k^2)^{1/3}B$$

Hence,

$$A = (6\omega_B)^{2/3}\omega/8 \quad (43a)$$
$$Y = (6\omega_B)^{5/6}/4 \quad (43b)$$

and

$$(V_p/k)B = (6\omega_B)^{5/3}/6 \quad (43c)$$

I. Critical Burn-Off

The flashing-interface temperature ψ_i is calculated from eqs 37, 38 or 39 by putting $A = 0$. It is more convenient to show ψ_i as a function of the equivalent time, Y. In general,

$$Y = (V_p/4)\,(t/k)^{1/2}$$

where V_p, the platen velocity, is equal to U (constant), g_pt, or $3at^2$ for, respectively, linear, parabolic or cubic flashing. As shown in Fig. 14, the interface temperature in each case seems to approach a definite value after sufficient elapse of time. However, far before this limiting value is reached, the interface reaches its melting point, i.e.,

$$\psi_i = 1 \text{ at } Y = Y_c$$

The critical values of Y, or Y_c, are indicated in Fig. 14 as A, B and C for the respective flashing patterns.

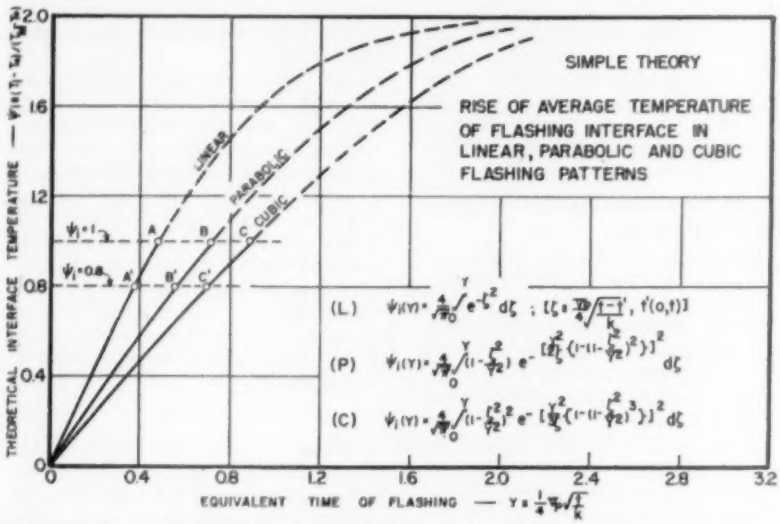
THEORETICAL INTERFACE TEMPERATURE — $\psi_i(Y) = (T_i - T_0)/(T_m - T_0)$
EQUIVALENT TIME OF FLASHING — $Y = \frac{1}{4}\sqrt{\frac{t}{k}}$
SIMPLE THEORY
RISE OF AVERAGE TEMPERATURE OF FLASHING INTERFACE IN LINEAR, PARABOLIC AND CUBIC FLASHING PATTERNS
LINEAR
PARABOLIC
CUBIC
A
B
C
A'
B'
C'
$\psi_i = 1$
$\psi_i = 0.8$
(L) $\psi_i(Y) = \frac{4}{\sqrt{\pi}}\int_0^Y e^{-\zeta^2}\,d\zeta$; $[\zeta = \frac{Y\xi}{4\sqrt{1-\xi^2}},\ f'(0,t)]$
(P) $\psi_i(Y) = \frac{4}{\sqrt{\pi}}\int_0^Y (1-\frac{\zeta^2}{Y^2})^2\, e^{-[\frac{Y^2}{3\zeta}\{1-(1-\frac{\zeta^2}{Y^2})^3\}]^2} d\zeta$
(C) $\psi_i(Y) = \frac{4}{\sqrt{\pi}}\int_0^Y (1-\frac{\zeta^2}{Y^2})^3\, e^{-[\frac{Y^2}{3\zeta}\{1-(1-\frac{\zeta^2}{Y^2})^3\}]^2} d\zeta$

Fig. 14 Theoretical flashing interface temperature as a function of equivalent time in linear, parabolic and cubic flashing

In practical flashing, the specimen is also heated by the Joule effect of the flashing current, and accordingly, the critical burn-off will be slightly less than that corresponding to the points A, B or C. Although the exact magnitude of Joule heating is difficult to evaluate, it seems reasonable to assume that Joule heating is responsible for 20% of the interface-temperature rise, $T_m - T_0$. Accordingly, the nondimensional interface-temperature rise ψ_i due to flashing alone is 0.8. The corresponding values of Y_c are shown at points A', B' and C' in Fig. 14.

The interface temperature can also be shown as a function of the equivalent burn-off, as is done in Fig. 15. It is seen that whereas the interface temperature increases rather fast at the initiation of linear flashing, the rate of the temperature rise soon drops below that of parabolic or cubic pattern as the flashing progresses. This phenomenon supports the explanation given in §7, to the fact that a longer critical burn-off is required to establish the stabilized temperature distribution in linear than in parabolic flashing. In the following table are given some critical values taken from Figs. 14 and 15:

$$P_{B_c} = (na/k^n)^{1/(2n-1)}\, B_c$$

and

$$(V_p/k)B_c = P_{B_c}(2nP_{B_c})^{(n-1)/n}$$

The values of $(V_p/k)B_c$ are seen to be almost identical for all flashing patterns, indicating that in flashing a given material with different patterns, the value of the critical burn-off is inversely proportional to the value of the critical platen velocity. This necessitates a longer critical burn-off in linear than in parabolic flashing since, as explained in §7, only relatively low platen velocities can be used in linear flashing.

The table also shows that the critical burn-off in parabolic flashing is given by the simplified theory to be

$$(g_p/k^2)^{1/3} B_c = 1.00 \quad (44)$$

When the Joule heating is considered, it is

$$(g_p/k^2)^{1/3} B_c = 0.72 \quad (45)$$

which is in close agreement with the experimental value of 0.70, as shown in Fig. 9, §7.

J. Theoretical Stabilized Temperature Distribution

The average interface temperature reaches the melting point at the critical burn-off and remains at that temper-

Flashing pattern	n	ψ_i	Y_c	P_{B_c}	$(V_p/k)\,B_c$
Linear	1	1.00	0.477	1.82	1.82
		0.80*	0.371	1.10	1.10
Parabolic	2	1.00	0.707	1.00	2.00
		0.80*	0.553	0.72	1.22
Cubic	3	1.00	0.880	0.755	2.07
		0.80*	0.690	0.564	1.27

* When 0.2 ψ_i is assumed due to Joule heating.

where, in general (§2),

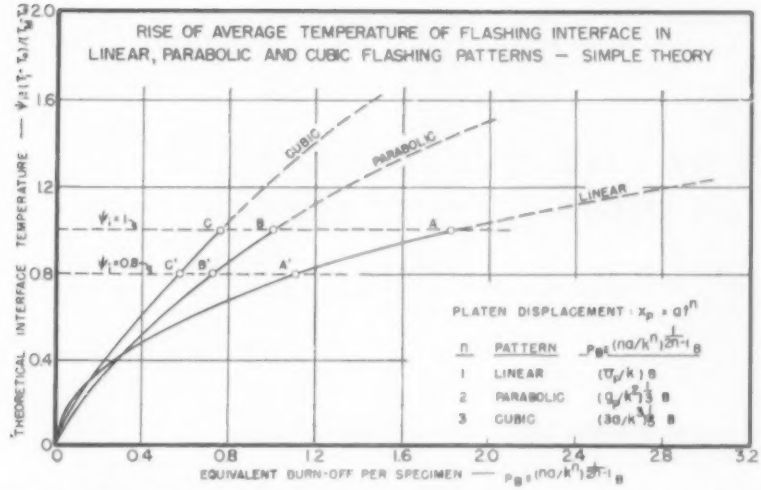

Fig. 15 Theoretical flashing interface temperature as a function of equivalent burn-off in linear, parabolic and cubic flashing

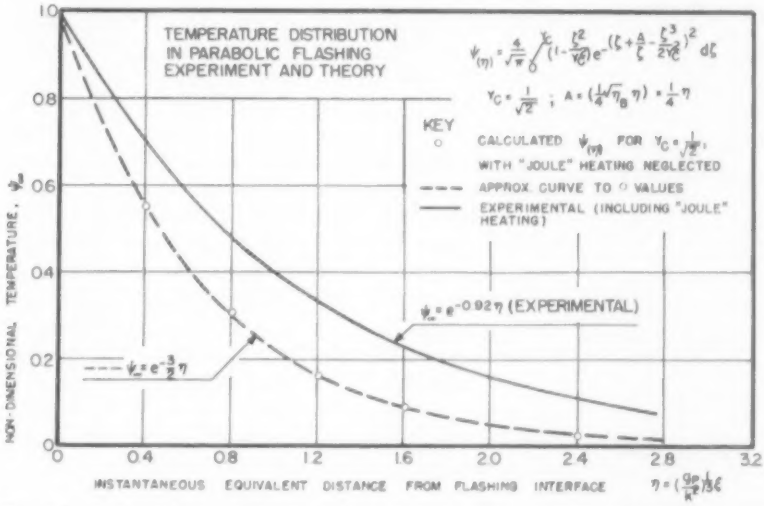

Fig. 16 Comparison of theoretical and corrected experimental temperature distribution in parabolic flashing

ature thereafter, causing the stabilization of the temperature distribution in the specimen. It may be assumed, and was observed experimentally, that the temperature distribution at the critical burn-off is a good approximation of the stabilized distribution.

Figure 16 shows a comparison between the theoretical and the experimental distribution for parabolic flashing. The circles represent the theoretical values computed graphically from eq 38 for $Y = Y_c = 0.707$, and are connected by the broken-lined curve which is closely described by the relation

$$\psi_\infty = e^{-1.5\eta} \qquad (46)$$

The solid curve shows the experimental distribution of $\psi_\infty = e^{-0.92\eta}$. The discrepancy between these two curves is believed to be a reflection of the neglection of Joule heating, and of the assumptions made with regard to the amount of heat dissipation in eq 35, the unidimensionality of heat flow, and the substitution of the stabilized distribution with the critical temperature distribution.

For linear flashing, calculation of eq 37 at the critical burn-off leads to a theoretical relationship

$$\psi_\infty = e^{-0.77\lambda}$$

There are few data to establish an experimental equation for this flashing pattern.

K. Effect of Finite Clamping Distance on Temperature Distribution

The effect of cooling by clamping jaws lowers the temperature distribution which, for a finite clamping distance, will be given for a certain constant temperature T_0' by

$$T - T_0' = (T_m - T_0')e^{-\alpha P}$$

Since $T = T_0$ at $P = P_1$, it follows that

$$T_0 - T_0' = (T_m - T_0')e^{-\alpha P_1} = (T_m - T_0')r$$

where $r \equiv (e^{-\alpha P_1})$ is the correction factor. From this, it can be shown readily

$$(T - T_0)/(T_m - T_0) = -r/(1 - r) + \psi_\infty/(1 - r)$$

or

$$\psi_\infty = \psi + r(1 - \psi) \qquad (47)$$

Since the stabilized temperature distribution is assumed to be equal to the critical temperature distribution, the distance l_1 from the interface (§5 and Fig. 11), at which T always equals T_0, may be evaluated by

$$l_1 = l_0 - B_c + l' \qquad (48)$$

where

$$l' = \nu D$$

Practically, a minor change in l' does not affect r appreciably for usual flashing specimen dimensions, and for a rectangular section of specimen with thickness D between the clamping jaws, the magnitude of ν may be taken to be of the order of unity, or $\nu = 1$. In this case the ratio of cooling area to the heat-conduction sectional area is $2 \times \nu D \times (\text{width})/D \times (\text{width}) = 2\nu = 2$.

For a round section of diameter D, the same ratio is given by

$$(\pi D \times \nu' D)/(\pi D^2/4) = 4\nu'$$

Physically, the values of l' will be determined so that the above ratio is equal in both sectional forms. Hence, $\nu' = 1/2$ for a round section.

Similarly, it can be shown readily that for tubular sections of outside diameter D and inside diameter d, $\nu'' = (D^2 - d^2)/2D^2$.

From these values of ν, the correction equations, eqs. 11 and 12 in §5, can be evaluated.

CONSIDERATIONS IN THE EVALUATION OF GRAPHITIZATION IN PIPING SYSTEMS

Laboratory investigations of graphitized piping, sampling, bend and impact testing, and metallographic grading; interpretation of test results; number of specimens required; effects of heat treatments

BY HELMUT THIELSCH, E. M. PHILLIPS AND E. R. JEROME, JR.

Introduction

Graphitization is still a problem of major concern to almost all power companies operating steam stations with carbon or carbon-moly piping, valves, headers, saddles, etc. New cases of serious graphitization are constantly found as a result of sampling programs in joints which had not been sampled previously as well as in joints which in earlier examinations appeared to be free of graphitization or contained only a mild, insignificant degree of graphitization.

In a number of instances potential failures in main steam piping joints have been avoided by a timely graphitization study. In several cases cracks have been found in the graphitized plane penetrating the wall thickness to within $^1/_4$ in. Since such cracks more commonly originate at the outside diameter, they are occasionally discovered by magnetic particle inspection techniques.

Laboratory exposure tests of carbon and carbon-moly steels at elevated temperatures have not provided a satisfactory correlation to the types of graphitization observed in steam piping systems under actual operating conditions. Thus, proper evaluation of graphitization can be made only on the basis of extensive experience gained over many years in the examination of power piping materials.

Graphitization only embrittles a joint. Actual failure requires additional stresses of considerable magnitude. In almost all cases, failures in service of graphitized piping have been associated with mechanical or thermal* shock or fatigue. Such failures have been the result of either one severe shock causing sudden rupture across the whole thickness of the joint or fatigue resulting in a somewhat slower initiation and propagation of a crack through the graphitized zone.

This complicates interpretation since there is no quantitative measure which determines the point at which the degree of graphitization (i.e., the embrittlement in the graphitized zone) is sufficiently severe to require repair of the joint by grooving out the heat-affected zone and rewelding. Under one set of operating conditions, as at locations in the pipe line far removed from the boiler or from other connections where sudden water carry-over resulting in a severe thermal shock is most unlikely, a heavier degree of graphitization might be assumed to be acceptable than at locations where water carry-over is a possibility.

Another complication is a lack of standardization in the testing procedures. Investigators vary somewhat in their sampling and testing procedures and in the interpretation of their findings.

The evaluation and discussion of the various testing criteria and of the results of several hundred commercial investigations is the major purpose of this paper.

Sampling

The procedure most widely used in determining the presence of graphitization in steam piping systems consists of removing boat-shaped slices from the welded joints by means of the so-called "weld-prober" saw illustrated in Fig. 1. The slice is taken across the weld and includes at least $^1/_2$ to 1 in. of base metal at each side of the weld. When properly prepared, the weld-probe slice provides sufficient material for a metallographic examination and, at least, one bend test, Fig. 2.

Pipe, valve or header sections of such dimensions as to make attachment of the weld-prober saw difficult may require special sectioning procedures as drilling or hack-saw cutting a wedged-shaped segment out of the joint. The cavities from which the weld-probe samples have been removed are usually rewelded and stress relieved to conform to ASA and/or ASME Code requirements.

Testing

Most laboratory evaluations of graphitized piping consist of cutting suitable specimens for metallographic

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* Thermal fatigue and thermal shock are terms used to denote the effects of temperature changes or alternating exposures at higher and lower temperatures on the life of a material. The differences between thermal fatigue and thermal shock are primarily related to the rate change of temperature and the severity of the temperature gradient. Thus, when the service life is primarily determined by the number of thermal cycles, failure is generally ascribed to thermal fatigue. However, when the severity of the temperature gradient or the rate of change in temperature is the primary cause of failure, the failure should be ascribed to thermal shock.[1]

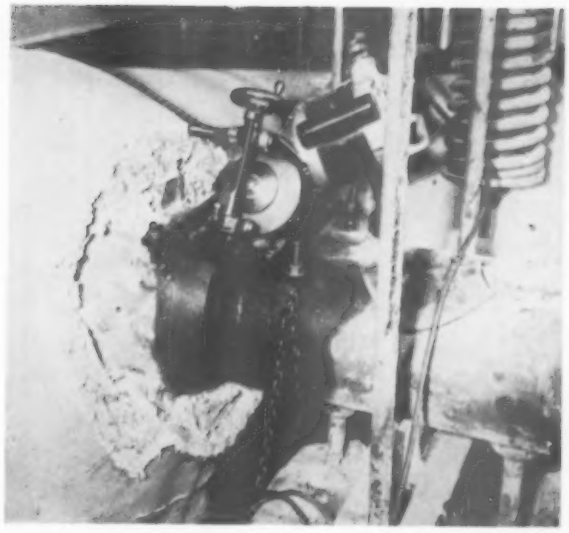


Fig. 1 "Weld-prober" saw in operation removing boat-shaped slice from main steam piping joint for laboratory evaluation

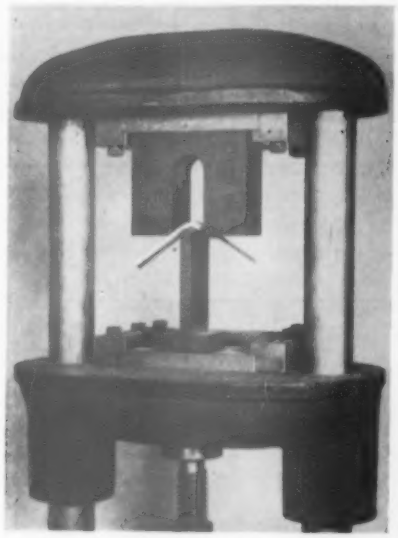


Fig. 3 Jig arrangement for guided bend test

examination and bend testing from the "weld-prober" segment.

No standard procedures have been established by any of the major technical societies concerned with power piping materials. Thus, there is some disagreement in the preparation of the test coupons, the testing procedures and the interpretation of the resulting test data as employed by different investigators. This increases the difficulty in comparing the results and interpretations obtained in different studies and/or by different investigators.

Bend Test

The slow-bend test is the mechanical test most commonly used to evaluate the level of graphitization in weld joints. Specimens generally are bent in a guided-bend test jig, as illustrated in Fig. 3.

There is considerable variation in the size of the specimens used. Specimen thicknesses may vary between $^1/_8$ and $^1/_4$ in. and widths between $^3/_8$ and $^1/_2$ in. Although the specimen length is not considered significant, a 3- to 4-in. length is most convenient.

Changes in the dimensions of the test jig introduce another variable. As a general rule it is recommended that the jig be designed to produce a bend radius of $r = 2t + {}^1/_{32}$ for specimens $^1/_8$ to $^1/_4$ in. thick[2] to $r = 2t + {}^1/_{16}$ for specimens $^1/_8$ to $^3/_{16}$ in. thick.

Bend-test results vary also with the location of the plunger in the jig in relation to the zone suspected of graphitization. Some investigators locate the pin perpendicular to the short axis of the specimen, i.e., the center of the plunger above the center of the zone suspected of graphitization as shown in Fig. 4*A*. Other investigators locate the plunger perpendicular to the plane suspected of graphitization as shown in Fig. 4*B*. The latter procedure represents the better practice and is followed by a majority of investigators.

Bending is usually continued until cracking occurs. In determining a quantitative value of the "degree" of graphitization some investigators use as criterion the angle of bend whereas others measure the elongation (usually in $^1/_2$ in.).

The bending angle or elongation may be measured when the first evidence of cracking is apparent, or after the crack extends across the width of the whole specimen. Many investigators measure the angle at the point where the crack has the initial length of $^1/_{32}$ or $^1/_{16}$ or $^1/_8$ in. Although it may be considered as arbitrary, a $^1/_{16}$-in. crack length is used now as the most satisfactory standard.

Because of the narrowness of the graphitized zone and the fact that the degree of graphitization may vary across the thickness of a joint, the proper evaluation of

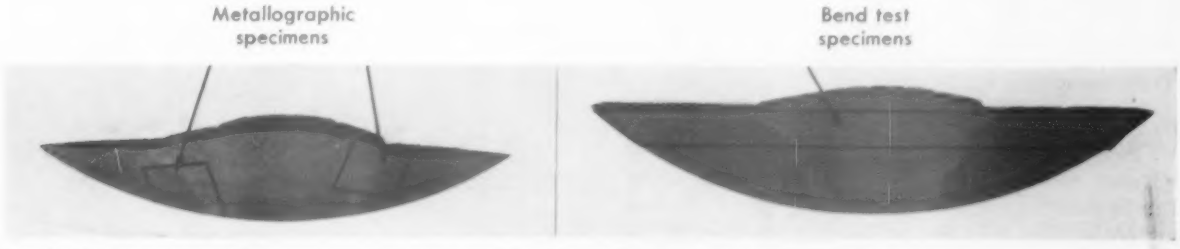


Fig. 2 Sections for metallographic examination and bend testing cut from boat-shaped slice

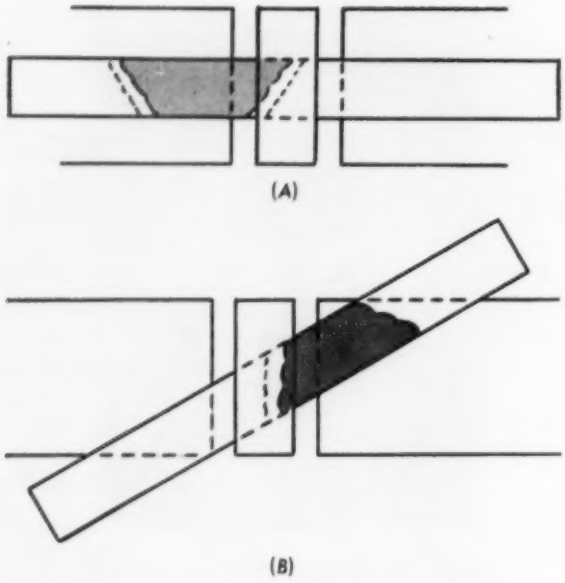


Fig. 4 Schematic sketch illustrating location of plunger in relation to graphitized plane. (A) Center of plunger perpendicular to short axis of bend specimen. (B) Center of plunger perpendicular to plane suspected of graphitization

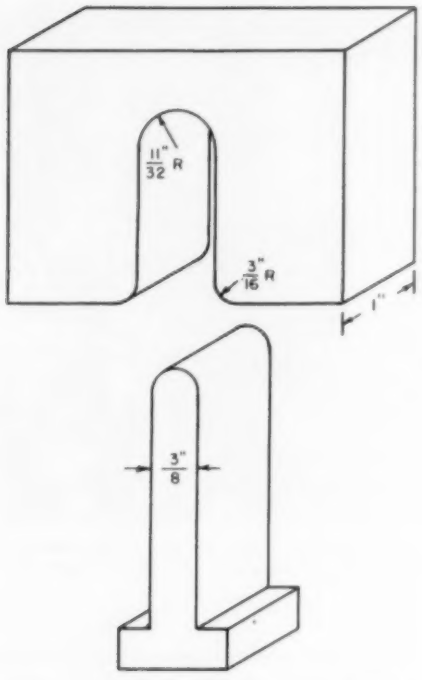


Fig. 5 Schematic sketch of standard test jig

these test variables is not readily made on graphitized specimens.

As an example, the scatter in test data of bend specimens across graphitized points is illustrated in Table 1 in which are evaluated the effects of variations in the dimensions of the test jig.

Table 1—Effects of Variations in Dimensions of Bend-Test Jigs Upon Bending Properties of Specimens* Removed from Graphitized Carbon-Moly Steel Piping

—Jig dimensions— Male (plunger) radius, in.	Female radius, in.	Bend angle at point where initial crack extended 1/16 in. deg.	Elongation at point where initial crack extended 1/16 in., % in 1/2 in.
1/4	7/16	10	..
3/16	11/32	8	3
1/8	11/32	5	3
1/16	1/4	2	..
1/4	7/16	3	..
3/16	11/32	8	3
1/8	11/32	11	3
1/16	1/4	3	..
1/4	7/16	30	6
3/16	11/32	25	6
1/8	11/32	21	6
1/16	1/4	10	3

* Specimens 1/8 x 5/16 x 3 1/2 in.

The specimens in each of the three groups were from adjacent areas of three severely graphitized joints. The considerable scatter in the test results prevents any conclusions in regard to the size effect of the jig dimensions.

In order to evaluate these test variables properly and arrive at a suitable procedure, a more homogeneous material is necessary. Thus several types of steels were given various heat treatments to simulate the various degrees of brittleness found in graphitized joints. The specimens were heat treated uniformly and tested to isolate the various testing variables.

On the basis of these results, a jig was selected with a plunger (male), 3/16 in. radius, and a female, 11/32 in. radius, leaving 1/32-in. clearance on each side of the specimen which is sufficient to prevent seizure of the bend specimen by the walls of the female member of the jig. A sketch of the jig is shown in Fig. 5.

A specimen thickness of 1/8 in. was selected as most satisfactory, particularly since occasionally only relatively narrow weld-probe slices are removed from graphitized pipe joints. The specimen is illustrated in Fig. 6.

Since some investigators prefer to determine elongation whereas others believe bending angle is a better criterion of the degree of graphitization, measurements of both have been taken in our research and commercial investigations.

Relation between bend angle and elongation is illustrated in Fig. 7, determined at the point where the initial crack length measures approximately 1/16 in.

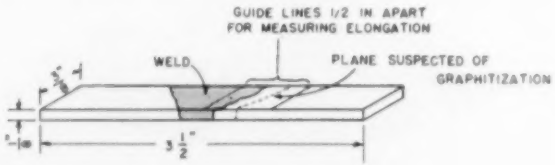


Fig. 6 Sketch of standard bend test specimen

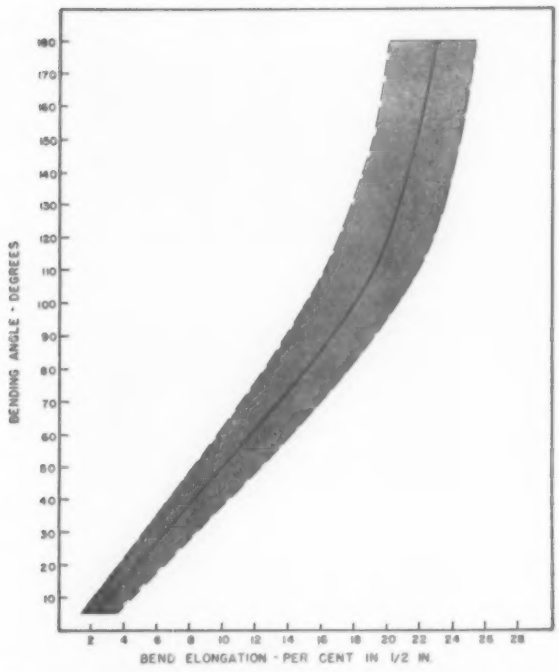


Fig. 7 Average relation of bending angle to elongation (determined at the angle where the initial crack length measures approximately $^1/_{16}$ in.)

The deviation from the mean curve may be as much as 20%. Since the possibility of error is somewhat greater in measuring the elongation than the bending angle, the latter seems the better criterion. It is preferred by most investigators.

Impact Toughness

Since embrittlement in steel is usually evaluated by means of impact tests, a series of tests was conducted to establish if any correlation could be obtained between impact and bend tests. Standard Charpy V-notch specimens were prepared with the root of the notch located in the graphitized plane as sketched in Fig. 8.

The results, plotted in Fig. 9, indicate that any correlation at best, is very poor. This may well be associated to the narrowness of the graphitized zone. As shown in Fig. 10, the fracture of several indentical specimens removed from the same joint and adjacent to each other did not follow the zone of graphitization. A severe embrittlement was indicated by the results of bend tests of specimens removed from the same joint.

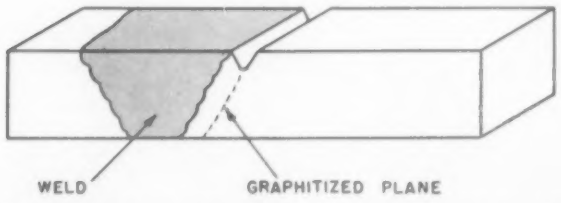


Fig. 8 Sketch of notch location in Charpy V-notch impact specimen

Metallographic Evaluation

Metallographic evaluation is most widely used to determine the degree of graphitization and to confirm the results of the guided-bend tests. The usual practice is to examine either the whole sectioned weld-probe specimen under the metallographic microscope or to cut smaller segments from this specimen representing heat-affected zone, base metal and weld metal. An initial examination is made at 100 diameters to determine the presence and amount of graphitization. The specimen is subsequently examined at 500 diameters to establish more clearly the degree and type of graphitization. Both magnifications are necessary for the proper interpretation of graphitization.

On the basis of 10 years of research in the laboratories of the Grinnell Co., a metallographic grading system of graphitization has been established which is illustrated in Fig. 11.[3]

The severity of graphitization depends upon the distribution, size and shape of the graphite particles. Whereas chain-type graphite, shown in Fig. 11*E*, usually indicates a "severe" or "extremely severe" condition, concentration of small nodular graphite particles (Fig. 11*F*) or a smaller number of very large nodular particles (Fig. 11 G) may be just as severe as the chain-type formation.

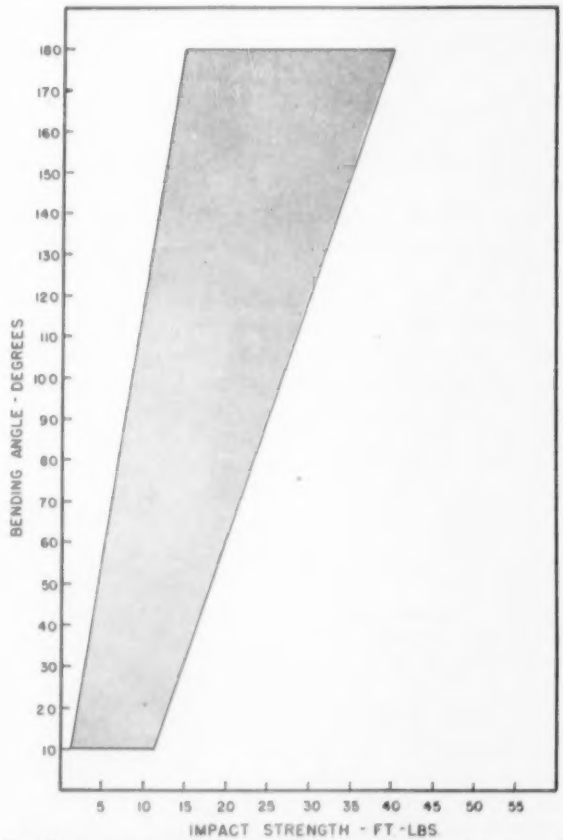


Fig. 9 Average relation between results of bend tests and impact tests. Specimens removed from adjacent areas in carbon-moly steel joints exhibiting different degrees of graphitization

The agreement between a careful metallographic evaluation and guided-bend tests is usually quite good. This is illustrated in Fig. 12 in which are summarized statistically the results of the various commercial investigations conducted during the past three years. By far the largest number of specimens exhibit either no graphitization or only very mild, mild or moderate degrees of graphitization. The microstructures shown in Fig. 11 normally correspond to the following bend angles:

Metallographic evaluation	*Bend angle, deg*
Very mild	Over 90
Mild	Over 90
Moderate	50–90
Heavy	30–50
Severe	15–30
Extremely severe	Below 15

Uniformity of Graphitization

One of the problems in setting up a sampling program is to decide on the number of samples which should be removed to provide a representative evaluation of a piping system.

A "safe" evaluation is considered by the Subgroup of the Metallurgy and Piping Subcommittee of the Edison Electric Institute[2] to be one in which three weld-probe samples are removed at 120-deg intervals around the circumference of each pipe or valve joint. Fewer weld-probe specimens are considered to be adequate only if the respective joint has been sampled previously and the variation in the degree of graphitization around the joint has been previously determined.[2]

Such extensive sampling programs are not always practical or economical. For example, where service conditions are such that graphitization is unlikely, a "spot" sampling program may be sufficient and serve as a preliminary basis for a more extensive program contemplated at some future date. To determine the variation in the degree of graphitization, approximately 1000 pipe-to-pipe and pipe-to-valve joints, sampled at two or three locations over the past five years in 30 typical steam stations, were analyzed statistically.

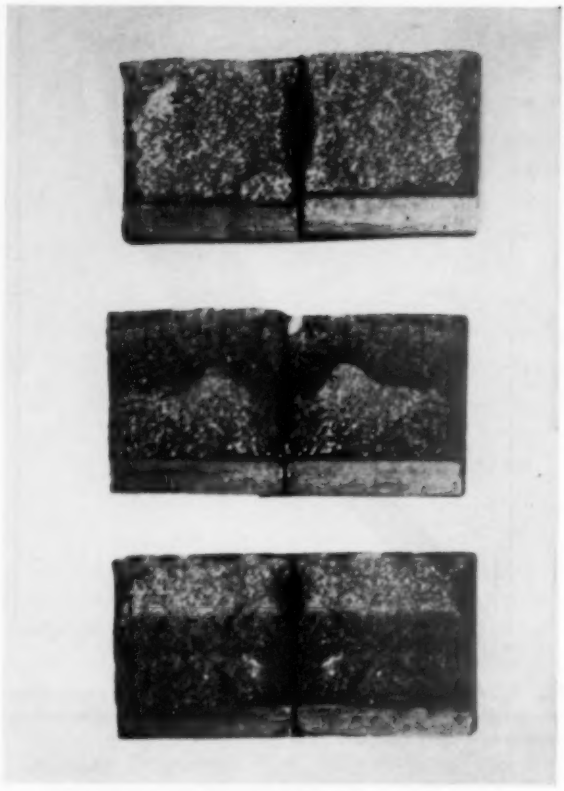


Fig. 10 Fracture surfaces of three Charpy V-notch impact specimens from adjacent areas of a severely graphitized carbon-moly valve steel

The results are summarized in Table 2. (Saddle, header, etc., are not included.)

Table 2—Variation in Degree of Graphitization in Carbon-Moly Steel Joints Sampled at Two or Three Locations as Determined by Metallographic Evaluation (None, Very Mild, Moderate, Heavy, Severe, Extremely Severe)

Structural component	*Separation in grading*					
	Same	*1 deg*	*2 deg*	*3 deg*	*4 deg*	*5 deg*
Pipe, %	85	10	4	1	0	0
Valve, %	75	12	8	4	1	0

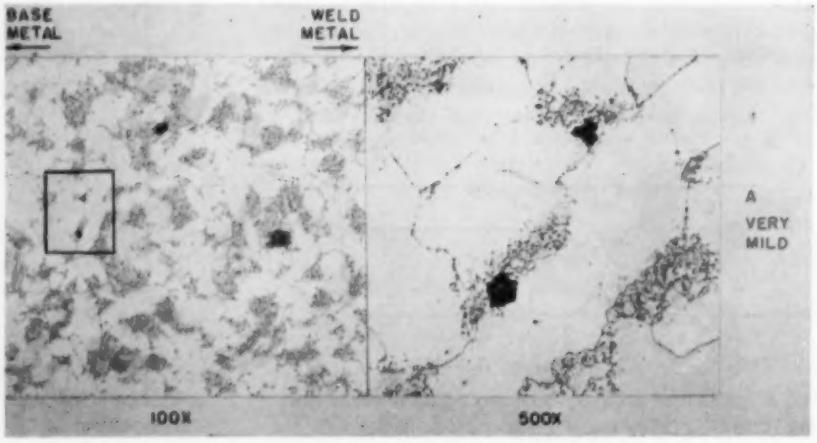
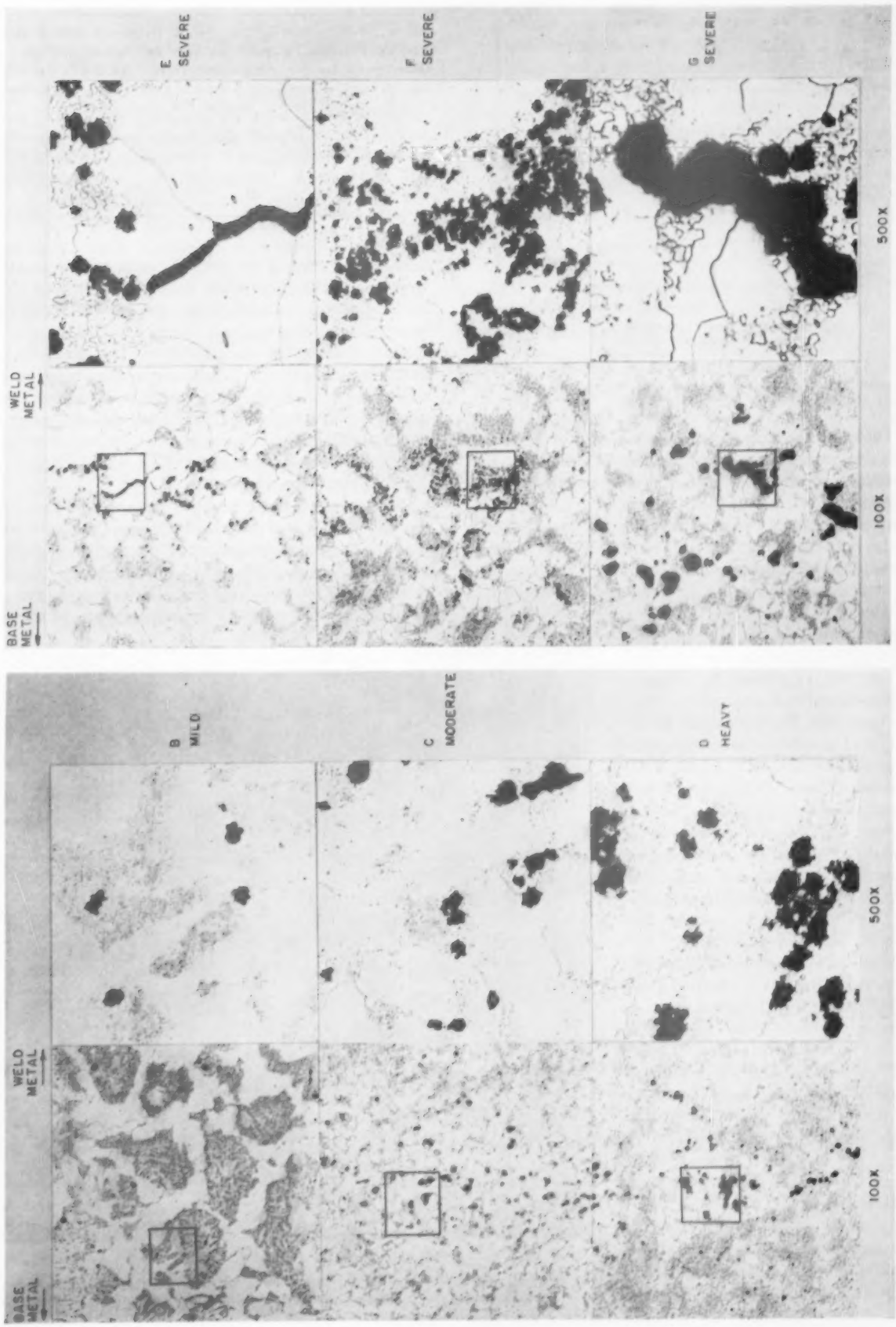


Fig. 11 Microstructures at 100 and 500 diameters illustrating various degrees of graphitization: (A) "very mild" graphitization; (B) "mild" graphitization; (C) "moderate" graphitization; (D) "heavy" graphitization; (E) "severe" graphitization; (F) "severe" graphitization; (G) "severe" graphitization. (Reduced by 1/3 upon reproduction)


B
MILD
C
MODERATE
D
HEAVY
E
SEVERE
F
SEVERE
G
SEVERE
WELD METAL
BASE METAL
100X
500X

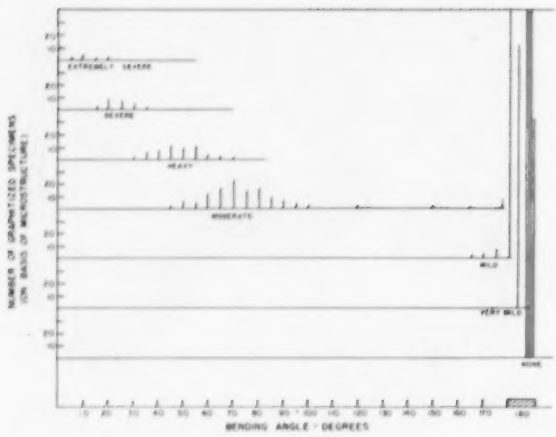


Fig. 12 Comparison of correlation between metallographic interpretation and bend test results of some 1700 weld-probe specimens

This shows that, in piping materials, 85% of the joints sampled by specimens at two or three different locations exhibited essentially the same degree (or absence) of graphitization around the periphery of the joint. In 10% of the joints the difference amounted to only about 1 deg; for example, the graphitization might have varied from none to very mild or from heavy to severe. In valve materials, the differences were somewhat greater.

If in this analysis all joints containing no graphitization are eliminated, the percentage variation increases, as summarized in Table 3.

Since the interpretation of the degree of graphitization often may be a borderline case, a 1-deg variation is not significant. Thus, these results show that in piping 15%, and in valves 30%, of the joints have a variation of two or more degrees which is significant; i.e., at one location the degree of graphitization may be "mild" whereas at another location the degree may be "heavy". An example of an extreme local variation is given in Fig. 13 illustrating "very mild" and "severe" degrees of graphitization in weld-probe samples removed 180 deg apart in a valve joint.

In some joints, the degree of graphitization varies also from the outside to the inside of the pipe or valve material. Where this variation occurs, the degree of graphitization tends to be more severe toward the outside diameter.

In most cases, the differences in the degree of graphitization probably can be associated with slight variations in the composition. This is particularly true in valves made of

Table 3—Variation in Degree of Graphitization in Carbon-Moly Joints Sampled at Two or Three Locations as Determined by Metallographic Tests. All Joints Containing at Least Some Graphitization (Very Mild, Mild, Moderate, Heavy, Severe, Extremely Severe)

Structural component	*Separation in grading*					
	Same	*1 deg*	*2 deg*	*3 deg*	*4 deg*	*5 deg*
Pipe, %	65	20	10	5	0	0
Valve, %	45	25	15	10	5	0

steel castings, which are more likely to exhibit greater variations in the composition than the hot-worked pipe steels. Moreover, castings, as a general rule, are likely to contain more carbon and aluminum than the corresponding grades of wrought and forged piping materials. In many foundries the 2-lb-aluminum-per-ton-of-steel addition needed for proper deoxidation is made in the runner box which, in rapidly solidified castings, may result in considerable variation in the deoxidation within the steel and a corresponding variation in the susceptibility to graphitization.

Differences in the stress, welding and postheating conditions may also have an effect upon the rate and resulting degree of graphitization. However, so far no satisfactory conclusion could be drawn as to the relative effect of each of these factors. The rate and type of graphitization also cannot be satisfactorily predicted

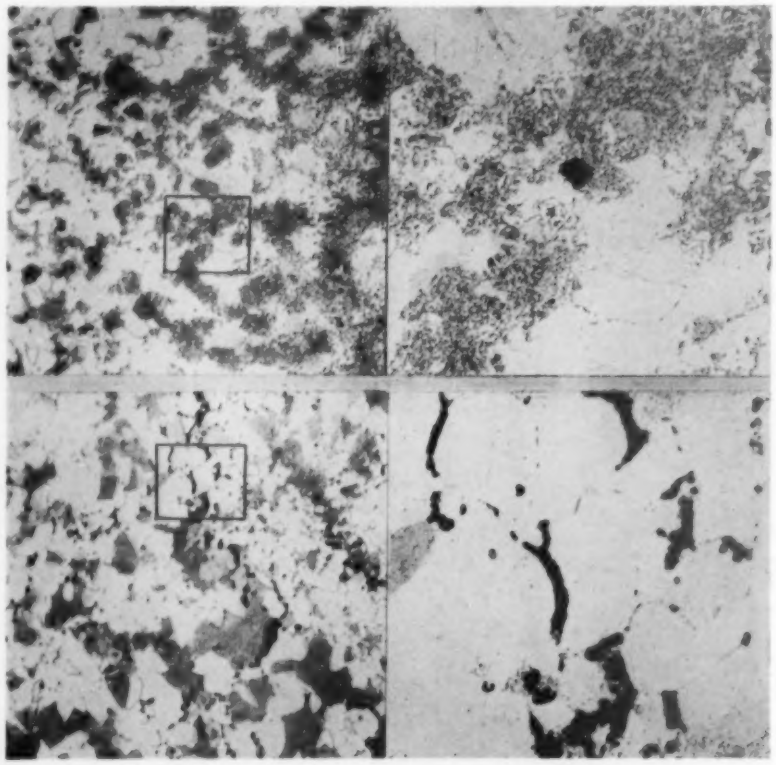


Fig. 13 Example of extreme variation in the degree of graphitization in two specimens removed 180 degrees apart from the same valve joint. (Reduced by 1/3 upon reproduction)

on the basis of carbon content, deoxidation practice, grain size, heat treatment, welding procedure, stress, etc.

As a result, the removal of two "weld-probe" specimens from each pipe and valve joint seems highly advisable. However, in piping from the same heat or from similar heats where on the basis of a few previous samples it has been indicated that graphitization might be absent, one subsequent weld-probe specimen per pipe joint seems more than adequate. There are occasions when it is sufficient to examine only one end of a pipe. Moreover, as mentioned earlier, where service conditions are such that graphitization is unlikely, a "spot" sampling program may be sufficient.

A preliminary investigation may serve also as basis for a periodic sampling program in which a limited number of samples are removed and examined every few years to ascertain continued absence of graphitization. In such a program samples should be removed from a few previously sampled and unsampled joints. Limited sampling is also sufficient on piping produced by certain steel mills from particular heats that have a low-aluminum deoxidation practice which as the basis of previous experience has been found not to be susceptible to significant graphitization. Similar comments are applicable to the valve castings produced by certain valve manufacturers.

Heat Treatment

Various heat treatments are involved in graphitization studies. Some of these are postweld heat treatments whereas others are special heat treatments.

Postweld Heat Treatments

Where new welds are made, two types of postweld heat treatments are now widely used to inhibit subsequent graphitization in the heat-affected zone.

Most commonly used is a metallurgical "subcritical stabilization" heat treatment consisting of heating the joint between 1300 and 1350°F for a minimum of 4 hr. This heat treatment inhibits or, at least, retards further graphitization. Reoccurrence of graphite in carbon-moly steel joints, when heat treated in this manner, if at all, usually is in the widely scattered small nodular form which does not cause significant embrittlement.

Another post weld heat treatment consists of heating the joint at 1650 to 1700° F for 2 hr. This heat treatment is also believed to suppress graphitization. It seems possible, however, that where local induction heating is used, a plane susceptible to graphitization may form in the pipe outside the area covered by the induction coils where the temperatures for the induction heating operation reach 1350 to 1425° F. To date no information is available on the effects of this postheat treatment upon subsequent graphitization.

Special Heat Treatments

Another heat treatment occasionally used on graphitized piping is the solution heat treatment which consists of heating the graphitized area to temperatures above the upper critical (transformation) temperature of the steel. At these temperatures the "ferritic" steel has transformed into an "austenitic" steel in which the graphite will go into solution. Upon subsequent cooling, some carbon will remain in "solution" while the balance forms cementite particles. A commercial solution heat treatment consists of heating for 2 to 4 hr at temperatures between 1700 and 1750° F, depending upon the type and degree of graphitization. This is followed by controlled cooling to 1000° F at a rate of 300 to 400° F per hour.

Although several years ago this heat treatment was believed to have considerable merit for "rehabilitating" graphitized joints, the usefulness of this method is now very much doubted. Particularly where operating periods of five or more years are involved, the solution heat treatment provides, at best, a temporary cure. It is believed that graphite will reform more rapidly in the solution heat-treated, heat-affected zone than it did in the originally stress-relieved weld. It is also believed that the solution of graphite during the solution heat treatment leaves very small submicroscopic voids in the steel matrix. These voids continue to act as local stress raisers so that the solution heat treatment improves only slightly the ductility and toughness over that of the previously graphitized area. This is illustrated in Table 4, in which are given the results of bend and impact tests.

Table 4—Bend Test and Impact (Charpy V-Notch) Test Data of Graphitized Carbon-Moly Steel Material (85,000 hr at 950° F). Prior and After Solution Heat Treatment (1 Hr at 1700° F—Cooled 300° F/Hr)

Bend angle, deg				
As-received	54	59	86	56
Solution heat treated	84	86	105	165
Impact strength, ft-lb				
As-received	8.5	9.0	9.2	11
Solution heat treated	12	13	14	15

Only where a mild degree of graphitization is apparent and the graphite particles are relatively small does the solution heat treatment seem to have merit and may suppress further graphitization. Data on this are now being collected.

Since the solution heat treatment is made by local induction heating, the possibility of setting up a plane susceptible to graphitization some distance away from the induction coils must also be considered. As mentioned earlier, no information has been collected to date on the effects of such heat treatments. It may be that by following the solution heat treatment with a "subcritical stabilization" heat treatment, subsequent serious graphitization is avoided.

Conclusions

Periodic weld-probe sampling of carbon and carbon-moly steel piping systems in service at temperatures exceeding 800° F should eliminate hazardous conditions due to graphitization.

The elimination of significant degrees of graphitization, or the absence of graphitization, does not insure that a high-temperature, high-pressure piping system is safe. Other types of defects, completely unrelated to graphitization are occasionally found. Some have resulted in serious cracking which, in time, would have resulted in complete failure of the affected joint.

Careful periodic inspection of all piping systems is therefore advisable in order to maintain a proper operational level of safety.

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RESEARCH NEWS

LEHIGH SUMMER COURSE:

Plastic Design in Structural Steel

During the past quarter-century, both in this country and abroad, research on the ultimate strength of steel structures has suggested challenging possibilities for the use of maximum (plastic) strength as a basis for structural design.

Traditionally, the measure of steel's total strength for construction purposes has been the largest load under which it is guaranteed to behave in a truly elastic manner. Present-day building codes, based upon the elastic concept, are logical and economical for individual beams and pin-connected frames.

When members are rigidly connected, however, the structure as a whole has long been known to possess a much greater load-carrying capacity than indicated by the elastic concept. These continuous (or "rigid") frames are able to carry increased loads above "first yield" because structural steel has the capacity to yield plastically with no loss of strength; indeed, with frequent increase in resistance. This behavior results when the rigidly connected, continuous structure draws upon the reserve strength of its less heavily stressed portions.

Sensing the values to our economy of a full exploitation of the more realistic measure of steel's strength, Lehigh University, Bethlehem, Pa., began in 1937 a series of research projects on rigid steel frames and their components which still continues. Since 1946 the studies have been under the sponsorship of the American Institute of Steel Construction, the American Iron and Steel Institute, the Welding Research Council and the Navy Department. The latter includes the Office of Naval Research, the Bureau of Ships, and the Bureau of Yards and Docks.

By the summer of this year, sufficient data will have been compiled to document the new design concept—calling upon the "ultimate" strength of structural steel as revealed by analysis of its plastic rather than elastic behavior under load.

The resulting techniques, already successfully applied in England, promise to produce substantial savings in cost through the more economic use of steel and savings in the design office made possible by the more simple plastic methods. At the same time, building frames will be more logically designed for greater over-all strength.

In order that educators in particular may have an opportunity to introduce these new concepts into their teaching at the earliest possible date, a special "summer" session has been arranged just prior to the opening of the fall college term: Sept. 7–15, 1955.

The course will review the behavior of steel structures loaded beyond the elastic limit and will present the fundamental concepts of plastic analysis. Specific plastic design techniques will be presented and examples worked out. The course will consist of a series of lectures and demonstrations, the latter including actual tests of structures to illustrate the principles. These tests, which will be carried through the elastic and plastic ranges, will include columns, continuous beams and full-sized connections of the type found in industrial buildings. Also, a complete frame will be tested, illustrating the accuracy with which the true strength of a steel frame—its "ultimate" strength—may be predicted.

Each of the lectures in the Summer Course will be an hour long with a 15-min. intermission between the two morning lectures. For those attending who are interested in the further discussion of particular points, informal discussion sessions will be arranged during the evenings at Fritz Laboratory.

All lectures will be given by members of the staff of the Fritz Engineering Laboratory who have participated extensively in this research investigation for the past several years.

For further information concerning the Course, inquiries should be addressed to: Dr. Lynn S. Beedle, Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pa.

Single Crystal Cylindrical Camera

A new Norelco Single Crystal Cylindrical Camera, designed to take oscillation and rotation photographs of single crystal specimens on a stationary cylindrical film, has been announced by the Research & Control Instruments Division, North American Philips Co., Inc., 750 S. Fulton Ave., Mount Vernon, N. Y.

The technique of rotating the crystal allows the investigator to determine the structure of crystal specimens through analysis of position and intensity of spots on the film. Horizontal spot patterns, called layer lines, reveal the nature of the lattice unit cell. When various reflections are indexed, it helps in the determination of the corresponding space group.

The new instrument is designed for mounting on the camera track of the Norelco Diffractometer and may be used in conjunction with the Norelco Flat Plate Camera.

THE EFFECTS OF ALLOYING ELEMENTS ON WELDS IN TITANIUM—PART II

A study of commercially important titanium alloying elements to determine how they affect the properties of welds in titanium

BY G. E. FAULKNER

ABSTRACT. Eighteen series of experimental titanium alloys, which contained additions of either aluminum or vanadium, or combined additions of two or more of most of the commercially important titanium alloying elements, were studied. The welding studies were conducted to determine the effects of alloying elements on the properties of welds in titanium alloys. Alloys which contained only the alpha-stabilizing element aluminum produced ductile welds with good notch toughness over the whole composition range studied. The ductility and notch toughness of welds in alloys which contained only the beta-stabilizing element vanadium generally became lower as the vanadium content increased and welds in some of the alloys were brittle. However, after heat treatment welds in all of the titanium-vanadium alloys were ductile.

Alloys which contained combinations of beta-stabilizing elements and aluminum appeared promising for welding applications. Aluminum additions increased the strength of alpha-beta alloys but had little effect on the properties of welds in these alloys. However, no combination of beta-stabilizing elements was found to produce alloys with base-metal and weld-joint properties superior to those of alloys which contained other beta-stabilizing elements.

Introduction

During the past two years, research has been conducted at Battelle Memorial Institute under the sponsorship of Watertown Arsenal* to study the effects of substitutional alloying elements on welds in titanium alloys. In the course of this investigation, experimental alloys which contained individual and combined additions of most of the commercially important titanium alloying elements were welded and tested. These tests were designed to show how the alloying elements affected the properties of welded joints in the experimental alloys. The alloying elements studied in the investigation were aluminum, chromium, iron, manganese, molybdenum and vanadium.

The effects of alloying elements on the transformation characteristics of titanium have been discussed extensively in the literature.[1-3] One group of elements, which includes aluminum, stabilizes the alpha phase of titanium (extends the alpha region of the titanium-alloy phase diagram). These elements are termed alpha-stabilizing elements. A second group stabilizes the beta form and includes chromium, iron, manganese, molybdenum and vanadium. These elements are termed beta-stabilizing elements. With these two groups of alloying elements, three different types of alloys may be formed, alpha, alpha-beta and beta alloys. All three types of alloys were included in this investigation.

In the first phase of this investigation, the beta-stabilizing elements—chromium, iron, manganese and molybdenum—were studied. A paper which presented the information obtained in the first phase of the investigation was published in THE WELDING JOURNAL.[4] This information is summarized below.

1. Alpha-beta alloys containing small amounts ($< 3\%$) of chromium, iron, manganese or molybdenum produced ductile weldments with good notch toughness in the as-welded condition and had maximum tensile strengths of about 110,000 psi.

2. Alpha-beta alloys containing over 3% of these elements generally had low ductility and notch toughness in the as-welded condition.

3. Heat treatments were beneficial in improving the ductility and notch toughness of welds in alloys containing up to 6% alloying elements.

4. Weldments in alloys which contained only the beta phase when quenched from the beta field had erratic properties and appeared to be affected by freezing segregation or contamination. For example, welds in a 13% chromium alloy were ductile as welded but were embrittled by a heat treatment designed to retain the beta phase of the alloy.

In the portion of the investigation covered by this paper, welding tests were made on series of alloys which contained: (1) the alpha-stabilizing element aluminum; (2) the beta-stabilizing element vanadium; (3) combinations of two or more beta-stabilizing elements and (4) combinations of one beta-stabilizing element and

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* Contract DA-33-019-OR D-231.

aluminum. These alloys also contained residual amounts of carbon, oxygen, nitrogen and hydrogen. However, the amount of these elements in the alloys was held as uniform as possible so as to limit the number of variables which would affect weld properties. The welding tests on these alloys were made to determine how aluminum and vanadium affected welded joints in titanium and to determine whether any combination of alloying elements produced alloys which had weld properties markedly superior to alloys containing other alloying elements.

Experimental Procedures

The alloy preparation, testing and welding procedures used in this investigation were described previously.[4] However, they will be reviewed briefly.

Alloy Preparation

The experimental alloys were prepared from titanium sponge and contain residual amounts of a number of elements. These elements include carbon, oxygen, nitrogen and hydrogen, and they have considerable effect on the mechanical properties of titanium when present in small amounts. Hydrogen, for example, is known to have appreciable effects on the notch toughness of alpha-titanium alloys, i.e., commercially pure titanium and titanium-aluminum alloys. In the alloys studied, the nitrogen content was approximately 0.021% and the carbon content was about 0.07 to 0.095%. No oxygen or hydrogen analyses were made.

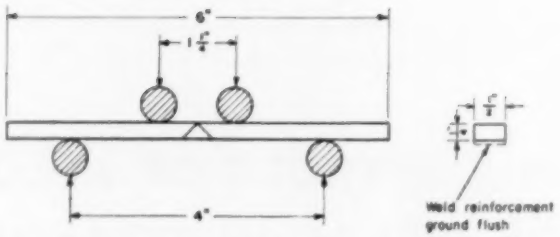


Fig. 2 Weld-joint two-point-loaded bend test

The oxygen and nitrogen levels are believed to be fairly low for alloys prepared from titanium sponge because the alloys were prepared from good-quality sponge and because precautions were taken to minimize the pickup of oxygen and nitrogen during melting and fabrication. An appreciable pickup of oxygen and nitrogen would have increased the tensile strength of the alloys with a loss of ductility and notch toughness.

Electrolytic chromium, iron, and manganese, powdered molybdenum, high-purity vanadium and commercial aluminum also were used in preparing the alloys. The alloys were prepared by double-melting procedures in a nonconsumable-electrode arc furnace. After being melted the second time, the alloy ingots were forged and rolled to 1/8-in. sheets and 1/4- and 1/2-in. plates for welding tests. Prior to welding, the plates were heat treated, grit blasted, pickled in a 4%

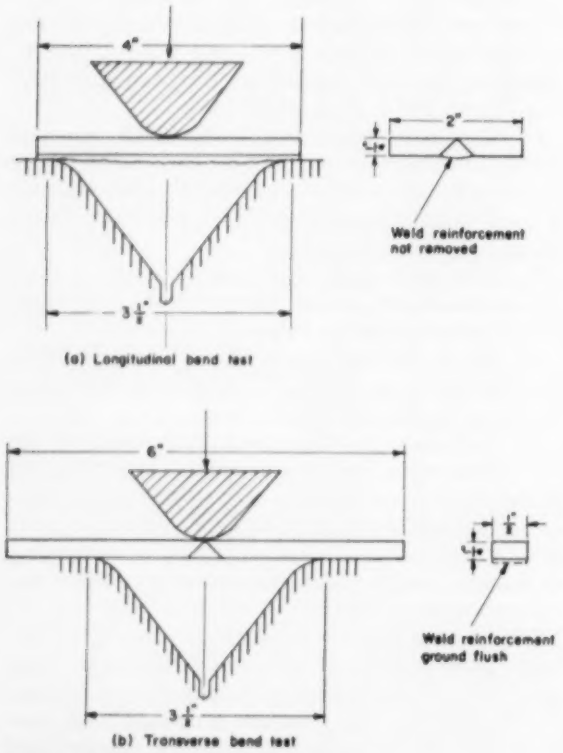


Fig. 1 Weld-joint guided-bend tests

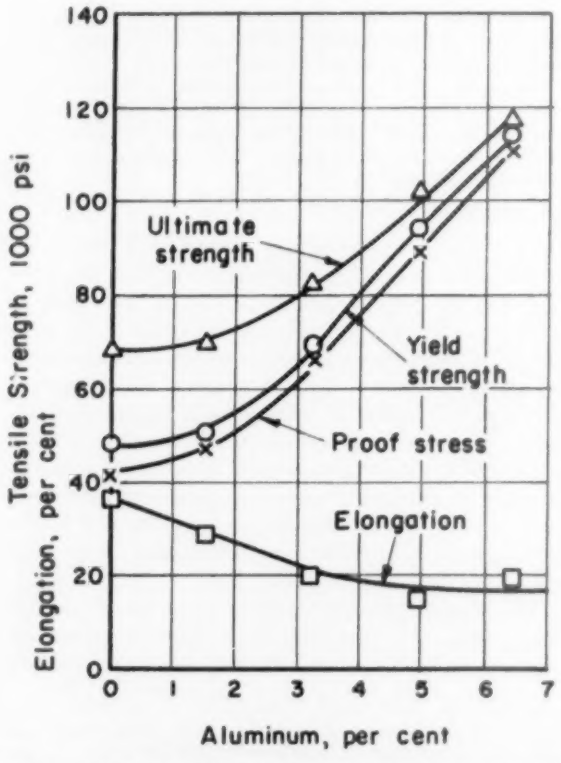


Fig. 3 Effect of aluminum on the mechanical properties of titanium

hydrofluoric acid–10% nitric acid solution, water rinsed and wire brushed. Plates thus prepared were shiny and appeared to be free from surface film.

Welding Procedures

All welds were made manually in a chamber filled with helium. Satisfactory welds probably could have been made outside of the welding chamber, but it was desired to limit the welding variables as much as practicable. Single-layer welds were made in the 1/8-in. sheet, two-layer welds were made in the 1/4-in. plates and four-layer welds were made in the 1/2-in. plates. Weld filler rods were of the same composition as the base metals and were obtained by shearing strips from the 1/8-in. alloy sheets.

Test Procedures

Tension tests were made on unwelded 1/8-in.-thick sheet. A flat-bar tension specimen was used with a 1/4-in.-wide reduced section 2 1/2 in. long. Elongation was measured over a 1-in. gage length.

The bend ductility of the base metals and welded joints was determined by making guided-bend tests. Two types of weld-joint guided-bend tests were made (Fig. 1). In the longitudinal-bend test, the specimens were bent so that maximum strain was parallel to the welding direction. Therefore, the weld metals, heat-affected zones and base metals had to deform together. In the transverse-bend tests, the specimens were bent so that maximum strain was normal to the direction of welding, and the three zones could deform separately. However, the specimens were bent so that failure was forced to occur in the weld metal.

To facilitate study of the data obtained from the guided-bend tests, the percent of elongation which occurred in the outer fiber of the bend specimen was determined. For the 1/8-in.-thick base-metal and weld-joint specimens, the bend elongation was calculated from the radius of the smallest die over which the specimen could be bent without failing. In the 1/4-in.-thick base-metal and weld-joint specimens, the bend elongation was measured by use of grid markings placed on the specimens prior to testing.

Another type of bend test was made on welded joints to determine the relative resistance of the weld metals, base metals and heat-affected zones to plastic flow (Fig. 2). In this test, a transverse-bend specimen was bent so that uniform bending moment was exerted across the welded joint. Because of the uniform bending moment exerted across the welded specimens, it was possible to determine the relative resistance of the weld zones and base metals to plastic flow by measuring the elongation which occurred in these zones during testing. The elongation was measured by the use of hardness impressions spaced at 0.03-in. intervals across the face of the weld. These impressions served as gage marks and the distance between them was measured before and after testing.

Notch-toughness tests were made on the 1/4- and 1/2-in.-thick weld metals and base metals. Standard vee-notch Charpy specimens were prepared from 1/2-in.-thick weld metals. Micro impact specimens were prepared from 1/4-in.-thick base metals and weld metals. The micro impact specimen gives values in inch-pounds similar to the values in foot-pounds obtained from vee-notch Charpy bars.

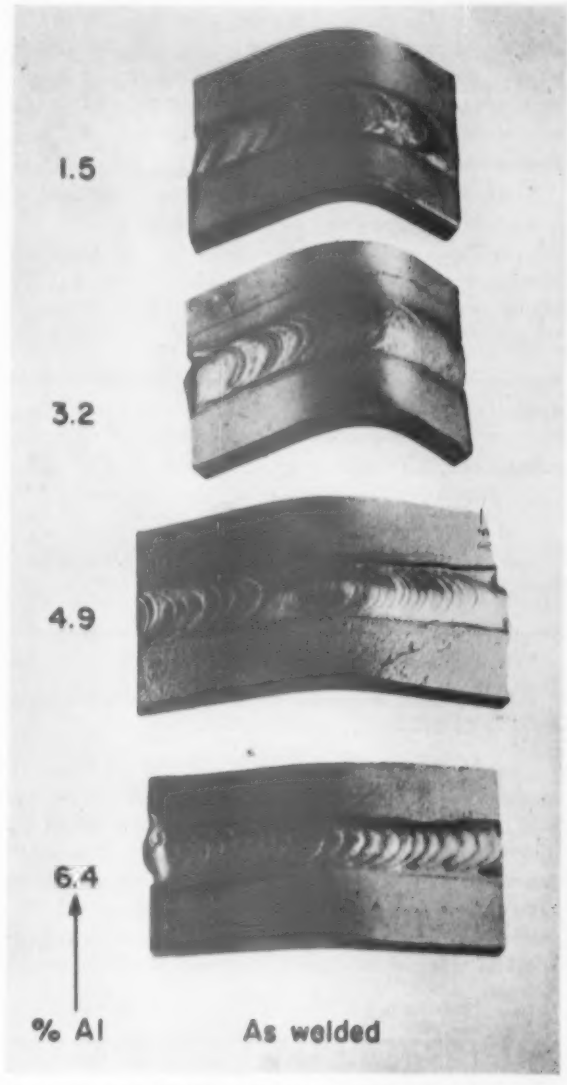


Fig. 4 Longitudinal bends in 1/4-in. titanium-aluminum welds

Hardness studies were made by taking Vickers hardness readings across the welded joints. The readings were taken in the base metals, weld metals and heat-affected zones. Metallographic examinations were made in conjunction with hardness studies.

Welded joints also were tested for aging. Aging consisted of storing welded specimens at room temperature and 250° F for 30 days. Effects of aging were determined by making longitudinal-bend tests and hardness tests on welded joints.

Discussion of Results

The results shown in this paper are in most cases from single tests. Single tests were believed to be satis-

factory for this work, because in studying series of alloys relationships are established that serve as a check on the individual test results. However, in notch-toughness tests two specimens were tested and the results shown are the average of the two tests.

Titanium-Aluminum Alloys

Tests on the titanium-aluminum alloys confirmed information obtained by other investigators.[5,6] The compositions and fabrication data for the titanium-aluminum alloys studied are listed in Table 1. The alloys became increasingly difficult to fabricate as their aluminum content was increased.

Table 1—Compositions of Experimental Titanium-Aluminum Alloys and Fabrication Data*

Aluminum content, %	*Forging temperature, ° F*	*Rolling temperature, ° F*
1.5	1700	1450
3.2	1800	1500
4.9	1900	1800
6.4†	1900	1800

* Prepared as two 10-lb ingots and fabricated to $^1/_2$-, $^1/_4$-, and $^1/_8$-in. plates.

† Cracked during forging and had to be machined before rolling, no $^1/_2$-in. plate prepared.

Tension Tests. The tensile strength of the titanium-aluminum alloys increased as their aluminum content was increased (Fig. 3). The ultimate strength of the alloys increased from 68,000 psi for the commercially pure titanium to 117,000 psi for the 6.4% aluminum alloy. The increased strength was accompanied by a decrease in elongation from 36 to 20%. To remove residual stresses due to rolling, the 6.4% alloy was heated to 1600° F for 1 hr and was air cooled. The other alloys were heated to 1500° F for 1 hr and were air cooled.

Bend Tests. Welds in all of the titanium-aluminum alloys had fairly good bend ductility. The bend ductility of weldments in the titanium-aluminum alloys is illustrated in the photograph in Fig. 4. The photograph shows weld-joint longitudinal-bend specimens. The specimens were tested in the as-welded condition. Data obtained from these $^1/_4$-in.-thick longitudinal-bend specimens and from $^1/_4$-in.-thick base-metal and weld-joint transverse-bend specimens are plotted in Fig. 5. The difference between the longitudinal-bend ductility and transverse-bend ductility of the welded joints is mainly due to the difference in width of the two specimens. Longitudinal-bend specimens were 2 in. wide, and the transverse-bend specimens and base-metal bend specimens were $^1/_2$ in. wide.

The data obtained from two-point-loaded bend tests made on welded joints in the titanium-aluminum alloys showed that the resistance of the base metals, weld metals and heat-affected zones to plastic flow was very similar. The similarity of plastic flow which occurred in all three zones indicated the lack of response that these alloys had to quench-type heat treatments.

Notch-Toughness Tests. The base-metal and weld-metal notch toughness of the titanium-aluminum alloys was relatively good for all compositions tested. This is shown in Fig. 6. The results shown are from micro impact specimens machined from $^1/_4$-in.-thick base metals and weld metals. A surprising fact was that the notch toughness of the weld metals was higher than that of the base metals. In most titanium alloys, the weld metals had lower notch toughness than the base metals. The notch toughness of the base metals

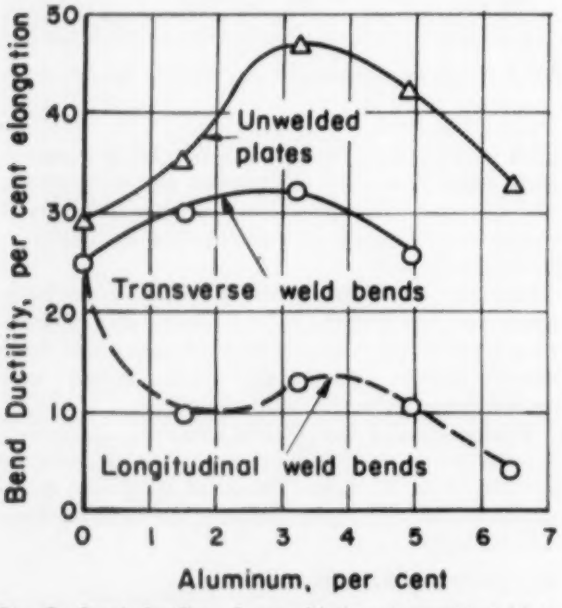


Fig. 5 Bend ductility of as-welded and unwelded $^1/_4$-in. plates

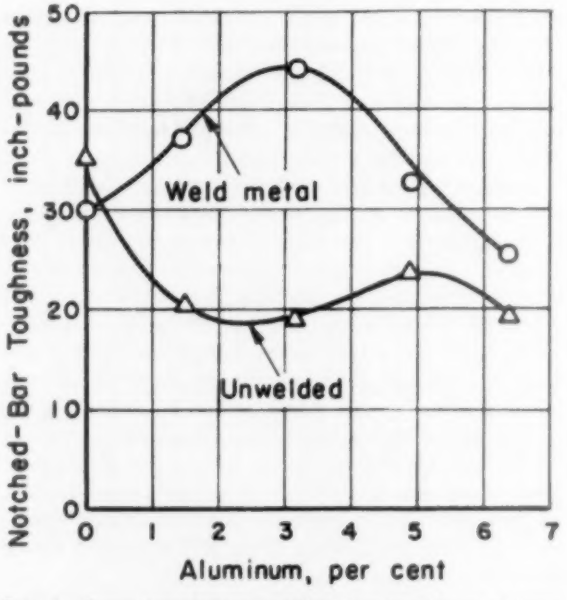


Fig. 6 Room-temperature notched-bar toughness of base metals and as-welded weld metals. Round-bar specimen, 0.225-in. diam, broken as a cantilever beam

at −40° F, as shown in Fig. 7, was lower than at room temperature but remained relatively good.

Standard vee-notch Charpy bars were prepared from weld metals in the four-layer welds in 1/2-in.-thick plates. The specimens were tested at room temperature and −40° F. The results are listed in Table 2. As was the case in base-metal tests, weld-metal notch toughness also was lower at −40° F than at room temperature, but remained relatively good for all of the alloys. The data from the weld-metal micro impact specimens prepared from welds in 1/4-in. plates are included for comparison purposes.

Table 2—Comparison Between Data from Micro Impact and Vee-Notch Charpy Specimens

	Weld-metal notch toughness		
	Inch-pounds*	Foot-pounds†	
Aluminum content, %	Room temp.	Room temp.	−40° F
1.5	37	51	45
3.2	44	40	39
4.9	33	35	28

* Micro impact specimen.
† Vee-notch Charpy specimens.

Hardness and Metallographic Studies. The hardness of the unaffected base metals, weld metals and heat-affected zones in weldments in the titanium-aluminum alloys are shown in Fig. 8. The hardness of the weld metals and heat-affected zones was higher than that of the base metals. However, this hardness increase was low and remained relatively uniform over the alloy range studied.

Photomicrographs of weld metals in the 3.2 and 4.9% aluminum alloys are shown in Fig. 9. These weld-metal structures were typical of those found in the other titanium-aluminum alloys and consisted of coarse serrated alpha plates which transformed from the beta phase while cooling from the welding temperatures.

Titanium-Vanadium Alloys

The compositions of the titanium-vanadium alloys prepared for this study and their forging and rolling temperatures are listed in Table 3. No difficulties were encountered in the fabrication of these alloys. However, the 20% alloy was prepared only as a 3-lb ingot and would have been more difficult to fabricate in a larger ingot size.

Table 3—Composition of Experimental Titanium-Vanadium Alloys and Fabrication Data*

Vanadium content, %	Forging temperature, ° F	Rolling temperature, ° F
1.0	1600	1400
2.9	1600	1400
5.2	1600	1400
9.8	1600	1400
11.7	1900†	1500
20.2‡	1750	1400

* Prepared as two 10-lb ingots and fabricated to 1/2-, 1/4- and 1/8-in. plates.
† Heated for forging in argon-filled retort.
‡ Prepared as 3-lb ingot, fabricated to 1/4- and 1/8-in. plate.

Heat Treatments. Two different heat treatments were used on the base metals and welded joints in the titanium-vanadium alloys. The heat treatment used depended on the vanadium content of the alloys.

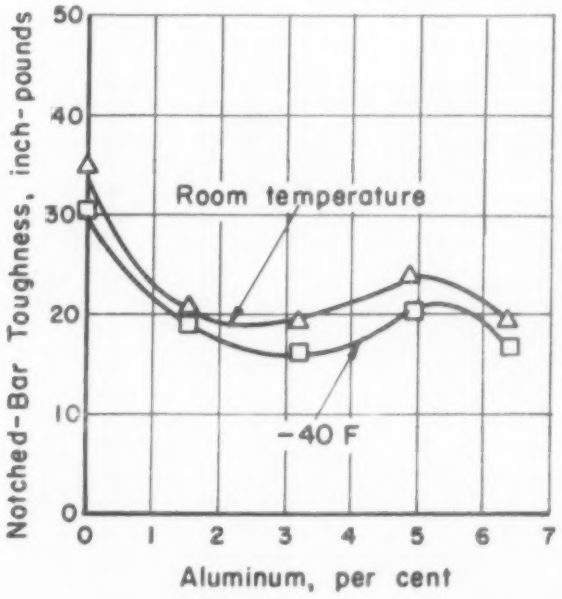


Fig. 7 Base-metal notched-bar toughness at room temperature and −40 F. Round-bar specimen, 0.225-in. diam, broken as a cantilever beam

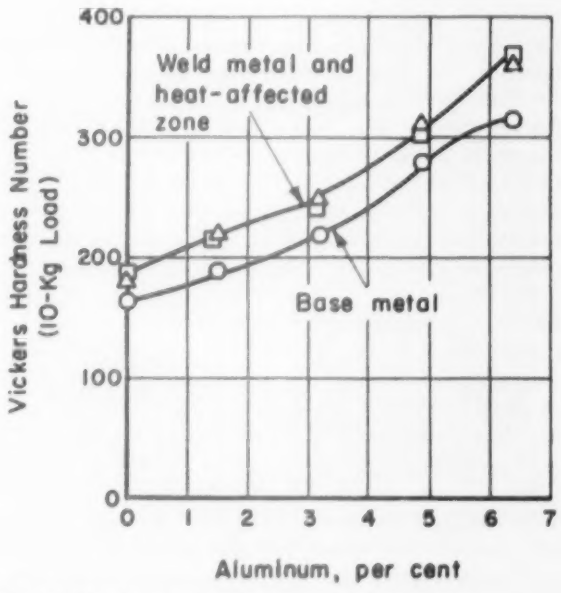
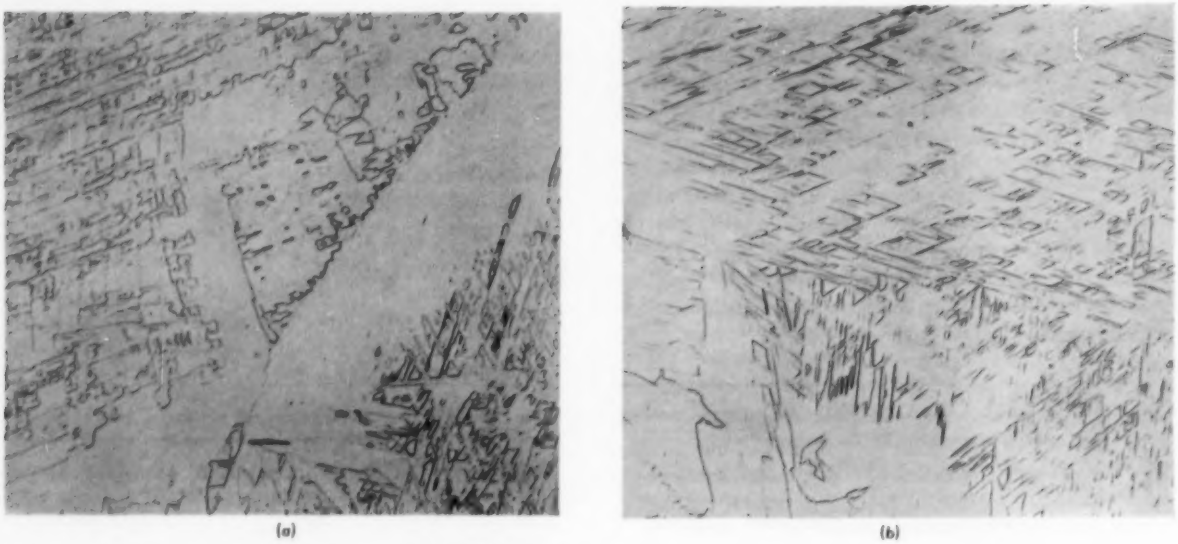


Fig. 8 Hardness values for welds in 1/8-in. plates



(a) (b)

Fig. 9 Weld-metal structures in the 3.2 and 4.9% aluminum alloys

(a) 3.2% aluminum as-welded, coarse serrated alpha transformation structure, Vhn 245. (b) 4.9% aluminum as-welded, coarse alpha transformation structure, Vhn 305. Etchant: $1^1/_2$ HF, $3^1/_2$ HNO_3, 95 H_2O. X 250.

In the alpha-beta heat treatment, which was used on alloys containing 1 to 9.8% vanadium, the base metals and welded joints were heated to 1350° F for 1 hr, furnace cooled to 1150° F and water quenched. This heat treatment was used on these alloys to remove residual stresses due to rolling and to overcome the transformation effects which occur in these alloys when they are rapidly cooled from rolling and welding temperatures. These transformation effects, which vary with alloy content and temperature, generally cause increased hardness and strength in the alloys and in some cases embrittlement of the alloys.

The initial holding temperature of 1350° F in the alpha-beta heat treatment is relatively high in the alpha-beta field of the titanium-vanadium alloy phase diagram. Holding the alloys at this temperature allows the structures to approach equilibrium during short holding times. The furnace cool to 1150° F causes a maximum amount of alpha rejection from the beta phase. This furnace cool lowers the hardness of the alloys and stabilizes the beta phase so that the alloys are less susceptible to aging. The water quench was used to prevent precipitation hardening from occurring in the alloys during the cool from 1150° F. In all probability, an air cool would have been sufficiently fast for this purpose.

In the beta-quench heat treatment, which was used on alloys containing 11.7 and 20% vanadium, the base metals and welded joints were heated into the beta field (1450° F) and water quenched. Alloys such as these contain practically all-beta structures when quenched from the beta field. They generally exhibit good strength and ductility in this condition.

Tension Tests. The effects of vanadium on the mechanical properties of titanium are shown in Fig. 10. The tensile strength of the titanium-vanadium alloys increased with increasing alloy content, reached a maximum for the 9.8% alloy, and then decreased. The ultimate strength increased from 69,500 psi for the 1% alloy to 129,800 psi for the 9.8% alloy, then decreased to 100,300 psi for the 20% alloy. Prior to testing, the 11.7 and 20% alloys were given the beta-quench heat treatment. The other alloys were given the alpha-beta heat treatment.

Bend Tests. The bend ductility of welds in the titanium-vanadium alloys changed considerably with

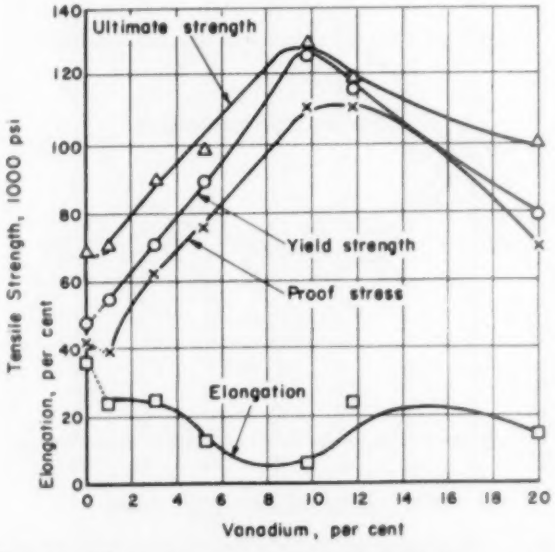


Fig. 10 Effect of vanadium on the mechanical properties of titanium

different alloy contents and in one case with different welding conditions. A photograph of the weld-joint longitudinal-bend specimens in $^1/_4$-in. plates is shown in Fig. 11. The results from base-metal and weld-joint longitudinal- and transverse-bend tests are plotted in Figs. 12 and 13. These results are from tests on $^1/_4$-in.-thick base metals and welded joints.

In the as-welded condition, the bend ductility of two-layer welds in the $^1/_4$-in.-thick alloy plates decreased rapidly with increasing vanadium content and was very low for welds in the alloys which contained more than 5.2% vanadium. With one exception, this was also true for the bend ductility of single-layer welds in $^1/_8$-in. sheets. The exception was the single-layer welds in the 20% alloy. Single-layer welds in this alloy were very ductile as welded, whereas two-layer welds were brittle. This difference in ductility between single- and two-layer joints is believed to be caused by the heating effect of the second weld layer or by the difference in cooling rates due to the different plate thicknesses.

After heat treatment, welds in all of the titanium-vanadium alloys were ductile. In the heat treatments, welds in the 1, 2.9, 5.2 and 9.8% alloys were subjected to the alpha-beta heat treatment. Welds in the 11.7 and 20% alloys were heated into the beta field (1450° F) and were water quenched.

The bend ductility of welds in titanium-vanadium alloys showed trends similar to those observed in alloys containing the other beta-stabilizing elements, chromium, iron, manganese and molybdenum. The important differences were that: (1) the bend ductility of welds in the 5.2% vanadium alloy was as good as the bend ductility of alloys containing only 3% chromium, iron and molybdenum; and (2) welds in all of the titanium-vanadium alloys were ductile after heat treatment, whereas welds in alloys containing over 6% of the other beta-stabilizing elements were brittle after heat treatment.

The results from two-point-loaded bend specimens indicated the response of the titanium-vanadium alloys to quench-type heat treatments. The weldments in these alloys had greater resistance to plastic flow than the base metals. This was true for both the as-welded and heat-treated conditions. However, the heat treatments which increased weld-joint bend ductility, decreased the resistance of the welds to plastic flow.

Notch-Toughness Tests. The effects of vanadium on base-metal and weld-metal notch toughness are shown in Fig. 14. These data are from micro impact specimens in $^1/_4$-in. base metals and welded joints. As the vanadium content of the alloys increased, the notch toughness of the base metals increased to a maximum for the 5.2% alloy, decreased to a minimum for the 11.7% alloy, and then increased for the 20% alloy. The 20% vanadium alloy is the first metastable-beta alloy studied in this investigation to have such good notch toughness.

Weld-metal notch toughness correlated with weld-joint bend ductility. Weld metals with low bend

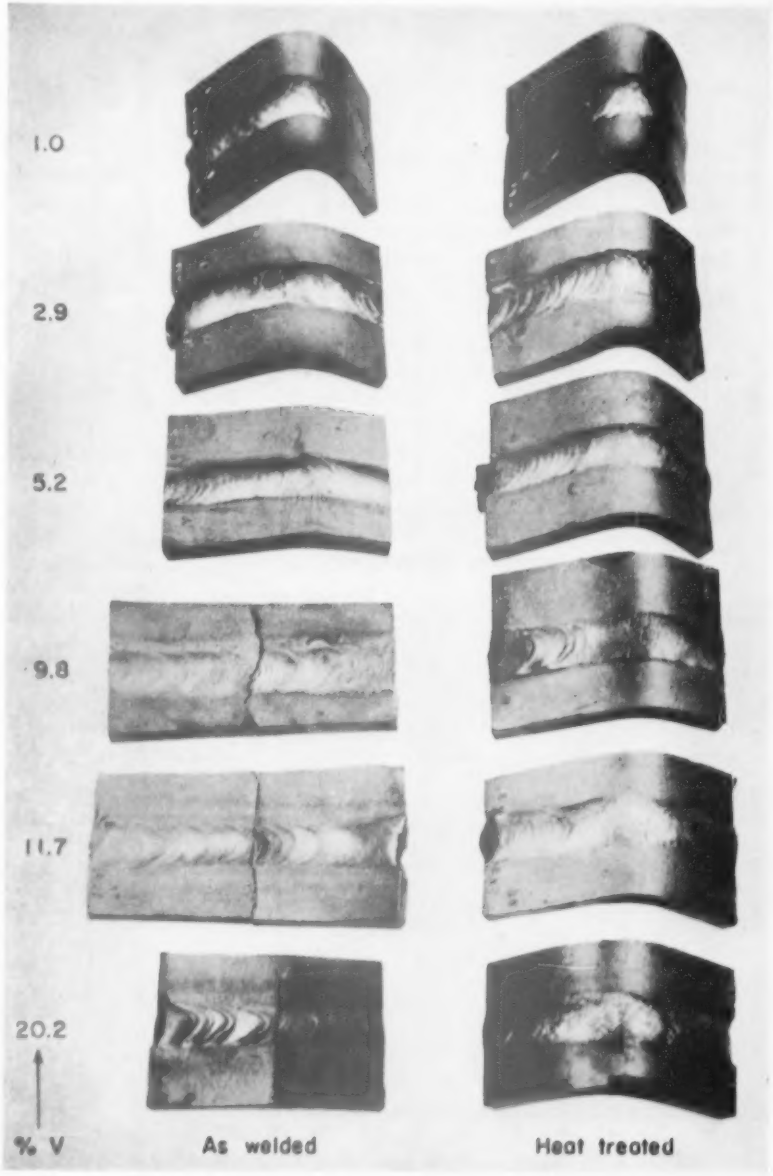


Fig. 11 Longitudinal bends in $^1/_4$-in. titanium-vanadium welds. Data shown in Fig. 12

ductility had low notch toughness, and weld metals with good bend ductility had good notch toughness. Heat treatment increased weld-metal notch toughness in most cases.

A comparison of base-metal notch toughness at room temperature and −40° F is shown in Fig. 15. The notch toughness of the 2.9 and 5.2% alloys was higher at −40° F than at room temperature; whereas, in the other alloys it was lower at −40° F. This latter effect has been observed in other titanium alloys. The notch toughness of the alloys remained relatively good at −40° F for all but the 11.7% alloy.

The results of notch-toughness tests on weld metal in four-layer welded joints in the $^1/_2$-in.-thick titanium-vanadium alloys are shown in Table 4. These tests were made using standard vee-notch Charpy specimens. Also included are data from micro impact specimens machined from two-layer welded joints in $^1/_4$-in. plate.

Table 4—Comparison Between Data from Micro Impact and Vee-Notch Charpy Specimens

Vanadium content, %	*Condition*	*Weld-metal notch toughness—Inch-pounds* Room temp.*	*Foot-pounds† Room temp.*	*Foot-pounds† −40° F*
1.0	As welded	35.5	38.5	34
2.9	As welded	35	37	41
5.2	Heat treated	31.5	37.5	28
9.8	Heat treated	20	12	12.5
11.7	Heat treated	17	4	..

* Micro impact specimen.
† Standard vee-notch Charpy specimens.

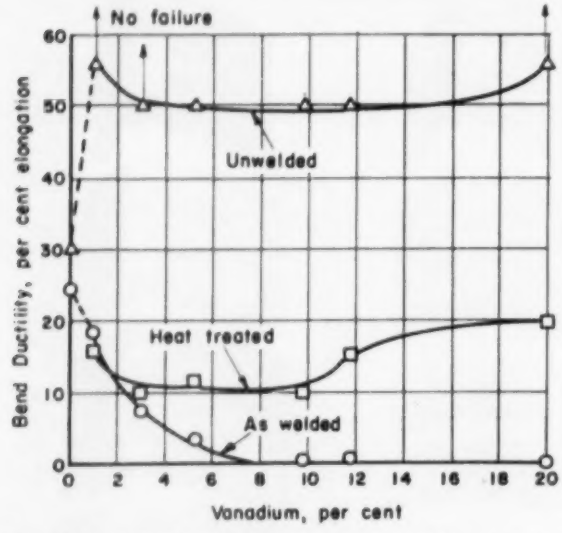


Fig. 12 Longitudinal bend ductility of welded and unwelded $^1/_4$-in. plates

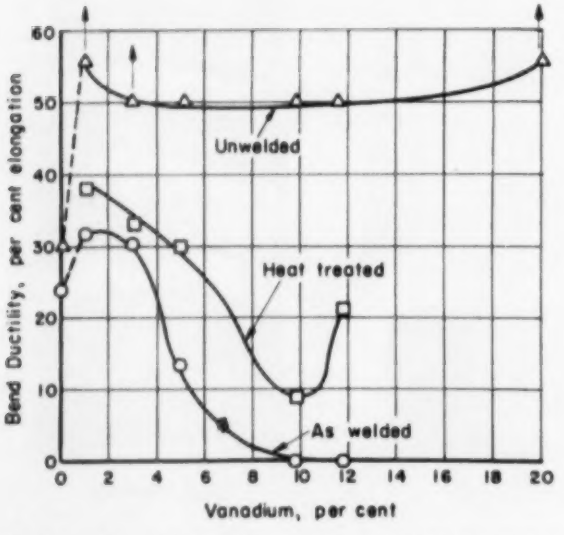


Fig. 13 Base-metal and transverse weld-joint bend ductility

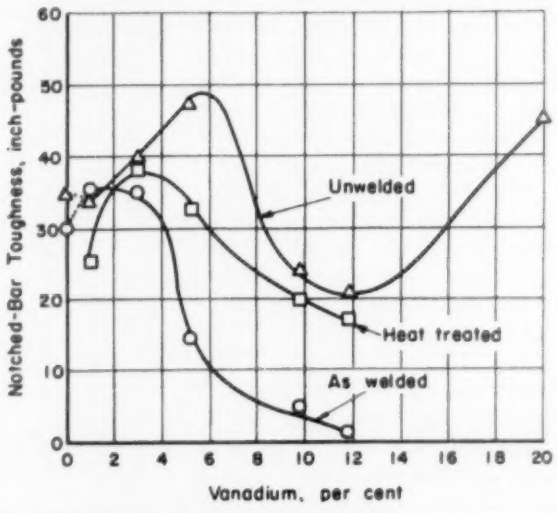


Fig. 14 Room-temperature notched-bar toughness of weld metals and base metals in $^1/_4$-in. plates. Round-bar specimens, 0.225-in. diam, broken as a cantilever beam

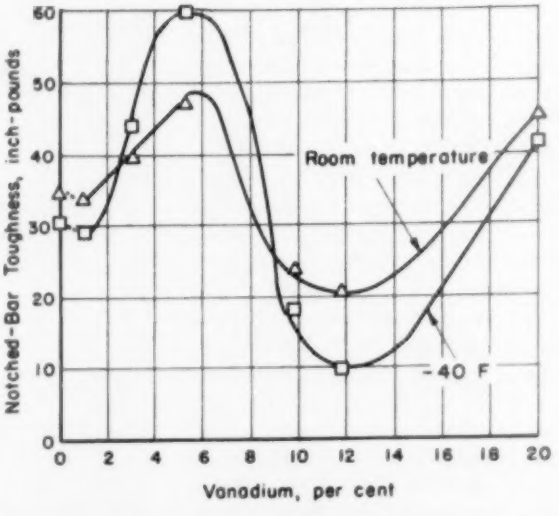


Fig. 15 Base-metal notched-bar toughness at room temperature and —40 F. Round-bar specimen, 0.225-in. diam, broken as a cantilever beam

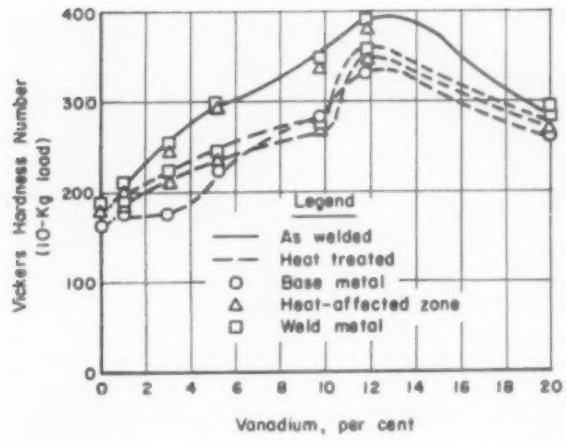


Fig. 16 Average hardness values for welded joints in $^1/_8$-in. plates

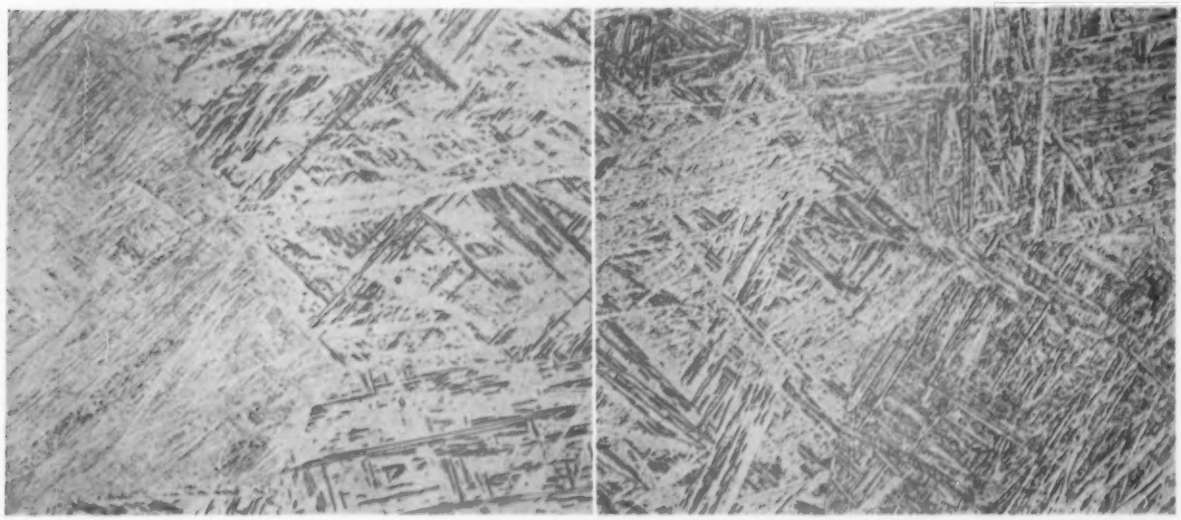
Hardness and Metallographic Studies. The average hardnesses of the base metals, weld metals and heat-affected zones of single-layer welds in the titanium-vanadium alloys are shown in Fig. 16. They are shown for weldments in the as-welded and heat-treated conditions. The hardness of the base metals increased uniformly with increasing vanadium content up through the 9.8% alloy, then increased drastically for the 11.7% alloy and decreased for the 20% alloy. In the 11.7 and 20% alloys, the base metals were given the beta-quench heat treatment. The high hardness in the 11.7% alloy base metal must have been caused by some hardening of this alloy by the rapid cool from the beta field. However, this alloy had higher bend ductility after being quenched from the beta field than it did when it was given the alpha-beta heat treatment. The alloys containing 1 to 9.8% vanadium were given this latter heat treatment.

The hardnesses of the weld metals and heat-affected zones in the as-welded condition were higher than the hardness of the base metals for most of the alloys. This hardening was due to the weld metals and heat-affected zones being heated into the beta field and rapidly cooled during the welding operation. However, heat treatment lowered the hardness of the weld metals and heat-affected zones until they approached the hardness of the base metals. In the case of the 20% vanadium alloy, welding and heat treatment had only a negligible effect on the hardness.

The weldments in the titanium-vanadium alloys had hardness trends similar to those observed in alloys containing the other beta-stabilizing elements. However, welding caused less hardening to occur in the weld metals and heat-affected zones of the vanadium alloys than the other alloys.

Photomicrographs of weld metals in some of the titanium-vanadium alloys are shown in Figs. 17 and 18. These were typical of the structures found in the weld metals of alloys containing the other beta-stabilizing elements. However, higher vanadium contents were generally required to produce a given structure. For example, the structure of the weld metal in the 3% vanadium alloy was similar to the weld-metal structure of alloys containing only 1% chromium, iron, manganese or molybdenum. Also, the weld-metal structure of the 10% vanadium alloy was similar to the structures of weld metals in alloys containing only 6% of these other elements.

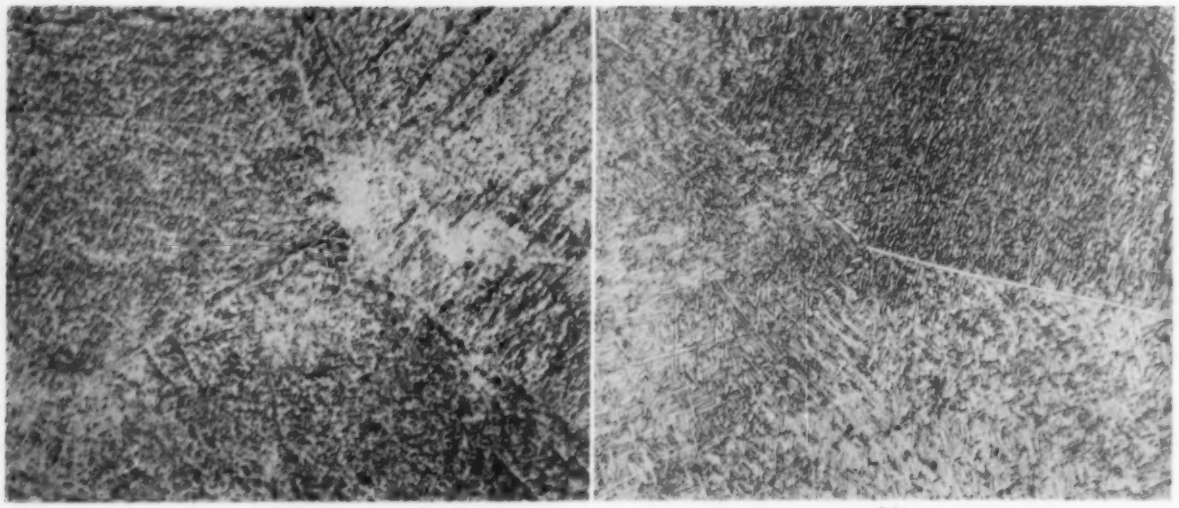
Photomicrographs of weld metals in the 20% vanadium alloy are shown in Fig. 19. They show the weld metal in the $^1/_8$-in.-thick sheet which was ductile as welded and the weld metal in the $^1/_4$-in.-thick plate which was brittle as welded. The structures and hard-



(a) (b)

Fig. 17 Weld-metal structures in the 3% vanadium alloy

(a) As-welded, coarse acicular transformed-beta structure, Vhn 257. X 250. (b) Alpha-beta heat treated, more Widmanstätten alpha transformed by heat treatment, Vhn 225. X 250.



(a) (b)

Fig. 18 Weld-metal structures in the 10% vanadium alloy

(a) As-welded, fine alpha precipitate in beta matrix, Vhn 345. (b) Alpha-beta heat treated, Widmanstätten alpha precipitated by heat treatment Vhn 283. Etchant: 1½ HF, 3½ HNO_3, 95 H_2O. X 250.

nesses in both welds were very similar.

Alloys Containing Combinations of Alloying Elements

All of the alloys prepared in this phase of the study contained beta-stabilizing elements in combination with aluminum or in combinations with one or more other beta-stabilizing elements. They were prepared in order to produce both alpha-beta and metastable-beta alloys.

Alpha-Beta Alloys. The compositions of the alpha-beta alloys prepared for this phase of the study and the forging and rolling temperatures used to fabricate them to plate are listed in Table 5. Generally, higher forging and rolling temperatures were used to fabricate the alloys which contained aluminum than were used for the alloys without aluminum additions.

About one-third of the experimental alpha-beta alloys were selected in order to have compositions which correspond with commercial alloys. The other alloys were selected to produce weldments with good bend ductility and notch toughness in the as-welded condition. In most cases, two or more alloys were studied which contained different amounts of the same alloying elements. These alloys were selected to show how increasing the alloy content affected weld-joint properties.

Aluminum was added in combination with the beta-stabilizing elements to determine the feasibility of using aluminum additions to increase the strength of alpha-beta alloys which produced ductile weldments. An aluminum addition of 3 to 4% was used in most of the alloys.

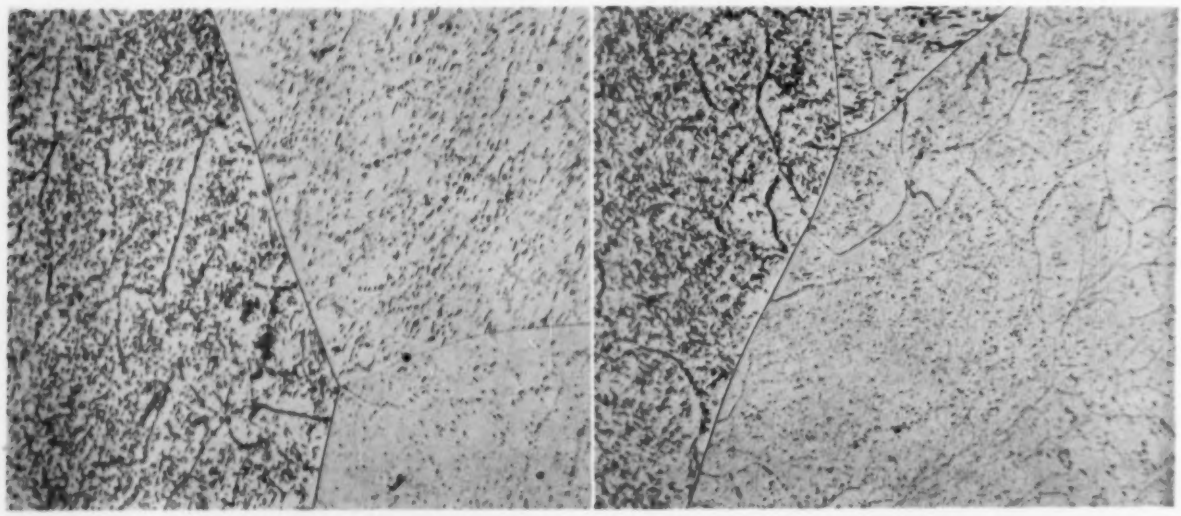


Fig. 19 Weld-metal structures in the 20% vanadium alloy

(a) ⅛-in. sheet, as-welded, ductile weld metal, Vhn 298. (b) ¼-in. plate, as-welded, brittle weld metal, Vhn 288. Etchant: 1½ HF, 3½ HNO_3 95 H_2O. X 250.

Table 5—Compositions of the Experimental Alpha-Beta Titanium Alloys and Fabrication Data*

Intended composition, % (balance titanium)†	*Actual composition, %†*	*Forging temperature, ° F*	*Rolling temperature ° F*
5Cr – 3Al	3.5Cr – 2.3Al	1850	1500
2Cr – 3Al	1.5Cr – 2.1Al	1850	1500
4Cr – 2Fe	3.6Cr – 1.9Fe	1850	1450
2.7Cr – 1.3Fe	2.2Cr – 1.5Fe	1800	1400
1Cr – 1Fe	0.9Cr – 1.0Fe	1750	1400
4Cr – 2Mo	3.7Cr – 1.5Mo	1850	1450
2Cr – 1Mo	1.8Cr – 0.5Mo	1800	1400
1Cr – 1Mo	0.9Cr – 0.5Mo	1800	1400
2Cr – 2Fe – 2Mo	1.7Cr – 1.7Fe – 1.5Mo	1850	1450
2Fe – 3Al	1.9Fe – 3.9Al	1800	1500
2Fe – 1V	1.9Fe – 0.9V	1750	1400
4Mn – 4Al	4Mn – 4Al	1800	1500
3Mn – 3Al	2.9Mn – 2.8Al	1800	1500
2Mn – 2Cr	1.9Mn – 1.85Cr	1750	1400
2Mn – 1Cr	1.9Mn – 0.95Cr	1750	1400
2Mn – 2Fe	2Mn – 1.95Fe	1750	1400
2Mn – 1Fe	1.9Mn – 1Fe	1750	1400
3Mn – 3V	2.35Mn – 2.7V	1750	1400
2Mn – 2V	1.65Mn – 2V	1750	1400
1Mo – 4Al	0.8Mo – 3.8Al	1800	1500
2Mo – 2V	1.6Mo – 2.0V	1750	1400
4V – 4Al	3.9V – 4.0Al	1800	1550
3V – 3Al	2.75V – 3.0Al	1800	1550

* Prepared as 15-lb ingots, fabricated to 1/2-, 1/4- and 1/8-in. plates.

† Also contained residual amounts of carbon, oxygen, nitrogen, hydrogen and other elements.

Heat-Treatment Tests: Heat-treatment tests were made on all of the alpha-beta alloys to select the heat treatments which would produce good weld-joint ductility. The type of heat treatment selected was one in which the welded specimens would not have to be heated to temperatures above the alpha-beta field. Heat treatments of this type had been used satisfactorily in the previous work.

The heat-treatment temperatures were selected on the basis of hardness studies. In the hardness studies, specimens of each alloy were used in both the as-rolled condition and as quenched from the beta field (2000° F). These specimens were heated to various temperatures in the alpha-beta field and water quenched. The temperature which produced a combination of low hardness in the as-rolled specimens and maximum softening in the beta-quenched specimens was selected for the heat-treatment study. This selected temperature ranged from 1300 to 1600° F and was generally highest for alloys which contained aluminum.

On the basis of the temperature selected from the heat-treatment tests, three heat treatments were selected for each alloy. These were:

1. The specimens were heated to the selected temperature (x° F), held 1 hr, furnace cooled over a range of about 200 to 300° F (x° F −250° F ±50° F), and water quenched.

2. The specimens were heated to the selected temperature (x° F), held 1 hr and water quenched.

3. The specimens were heated from 100 to 150° F above the selected temperature (x° F +125° F ± 25° F), held 1 hr and water quenched.

The heat treatments selected for each alloy are shown in Table A-1 in the Appendix.

With these three heat treatments, quenching temperatures ranging from 300° F below the selected temperature to 150° F above the selected temperature were obtained. These heat treatments were not optimum heat treatments for each alloy, but were used to obtain general information concerning the effects of heat treatments on the alloys.

The heat treatments were evaluated on the basis of base-metal bend ductility and tensile properties. Three bend specimens, 1/8 in. thick, were prepared from each alloy and given one of the three heat treatments. Also, one specimen from each alloy was heated into the beta field (2000° F), water quenched and then was given the No. 1 heat treatment. The results from bend tests on these specimens are listed in Table A-2 in the Appendix.

The heat-treatment tests showed:

1. The as-rolled specimens, which were given the No. 1 heat treatment, generally had the highest bend ductility, and as the quenching temperature was increased, bend ductility generally became lower.

2. The beta-treated specimens, which were subsequently given the No. 1 heat treatment, had appreciably lower ductility than the as-rolled specimens given the No. 1 heat treatment.

In the tension tests, the tensile strength of 1/8-in.-thick sheets of the alloys was determined after both the No. 1 and No. 2 heat treatments. These tests were made to determine whether or not the low alpha-beta alloys could be made to develop improved tensile properties by heat treatment. The tests were not made on specimens in the No. 3 heat-treated condition because it was felt that sufficient information was obtained from the specimens given the No. 1 and No. 2 heat treatments.

The results of tension tests on the alloys in the No. 1 and No. 2 heat-treated conditions are listed in Table A-3 in the Appendix. The results from the tests are fairly consistent and show that increasing the quenching temperature from x° F − 250° F ± 50° F to x° F increased the strength of the alloys from 5000 to 48,000 psi.

The effects of the heat treatments on bend ductility and tensile properties are in accord with information published by Holden, Ogden and Jaffee.[7] They reported that: (1) the tensile strength of alpha-beta alloys heat treated in the alpha-beta region of the phase diagram depended primarily on the alpha-phase to beta-phase ratio of the alloys; (2) the alpha-phase to beta-phase ratio could be changed by variations in quenching temperatures and alloy content; (3) grain size and shape had more effect on ductility than strength and (4) that aging tests could be used to strengthen alpha-beta titanium alloys.

The tests showed that alloys with composition ranges low enough to produce ductile weldments were respon-

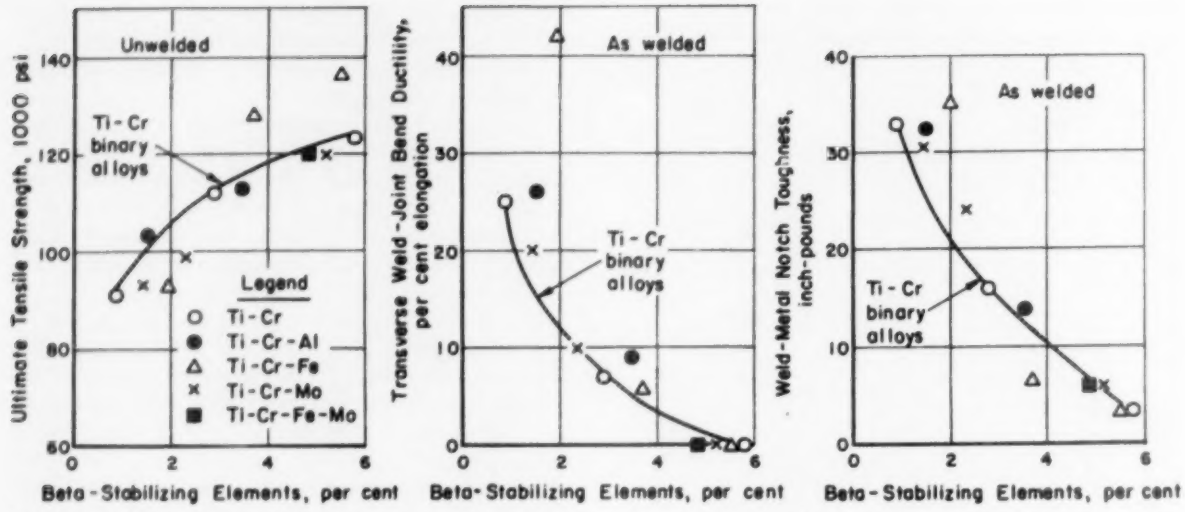


Fig. 20 Mechanical and weld-joint properties of titanium alloys containing chromium in combination with other alloying elements

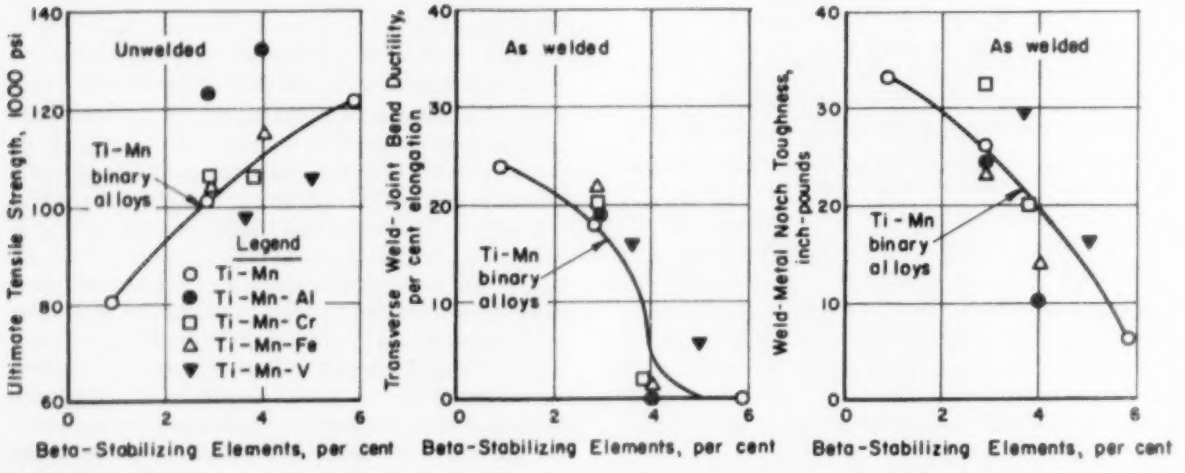


Fig. 21 Mechanical and weld-joint properties of titanium alloys containing manganese in combination with other alloying elements

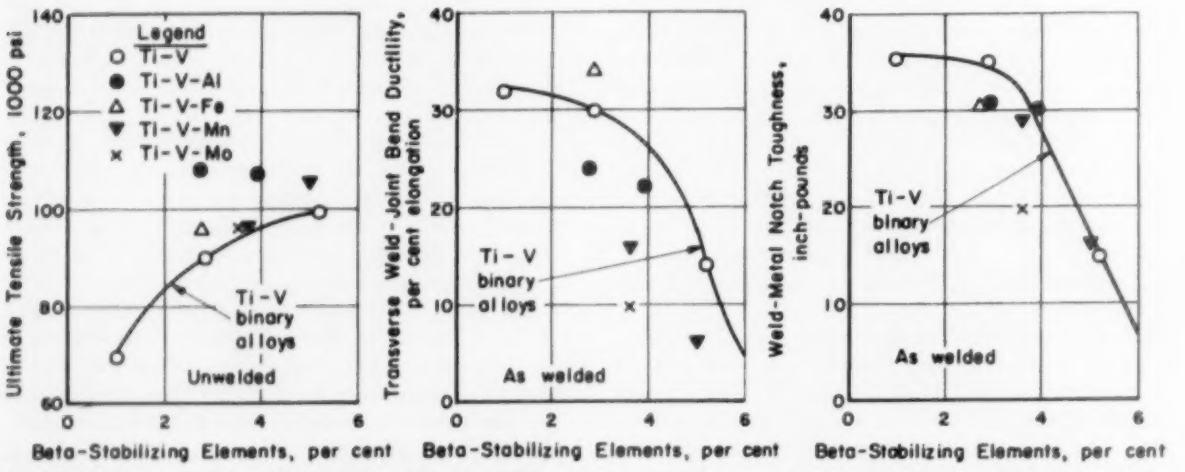


Fig. 22 Mechanical and weld-joint properties of titanium alloys containing vanadium in combination with other alloying elements

sive to heat treatment. It is possible that the strength of most of these alloys could be made higher by aging-type heat treatments.

Weld-Joint Properties: The bend ductility and notch toughness of the base metals and welded joints in the experimental alpha-beta alloys are listed in Table A-4 in the Appendix. These data are from $^1/_4$-in.-thick base metals and two-layer welded joints. The tensile properties of the $^1/_8$-in.-thick base metals in the No. 1 heat-treated conditions are included for comparison purposes.

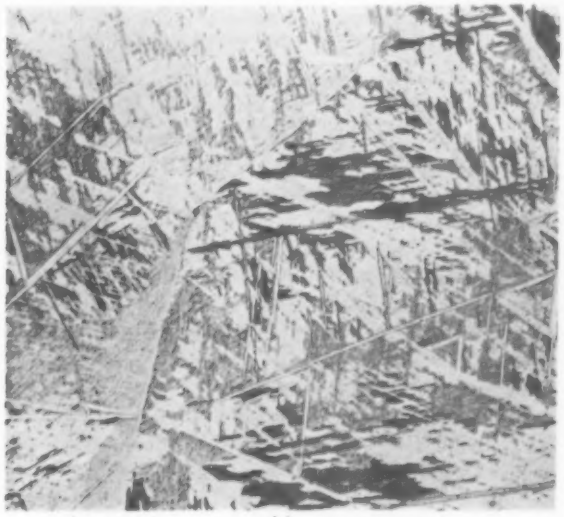
The effects of adding combinations of alloying elements to titanium alloys are shown graphically in Figs. 20, 21 and 22. These graphs were plotted to compare the properties of binary alloys with the properties of ternary and quaternary alloys containing combinations of alloying elements. To facilitate comparison, curves were drawn between the points representing the binary alloys. The data plotted in the curves include base-metal tensile strength and weld-joint bend ductility and notch toughness.

These curves show that:

1. Aluminum additions to the alpha-beta alloys generally increased the strength of the alloys without lowering their weld-joint bend ductility and notch toughness appreciably. In the alloys containing chromium, aluminum additions had only a slight effect on both the tensile strength and weld-joint properties.

2. Alpha-beta alloys containing combinations of two or more beta-stabilizing elements generally had base-metal and weld-joint properties similar to those of alloys containing only one beta-stabilizing element. No combination of beta-stabilizing elements appeared to produce alloys with markedly superior weld-joint properties than the other alloys.

The addition of aluminum to the alpha-beta alloys appeared to offer most promise for alloys which had a combination of good strength and good as-welded, weld-joint bend ductility and notch toughness. Alloys containing combinations of beta-stabilizing elements without aluminum additions generally had properties similar to binary alloys.



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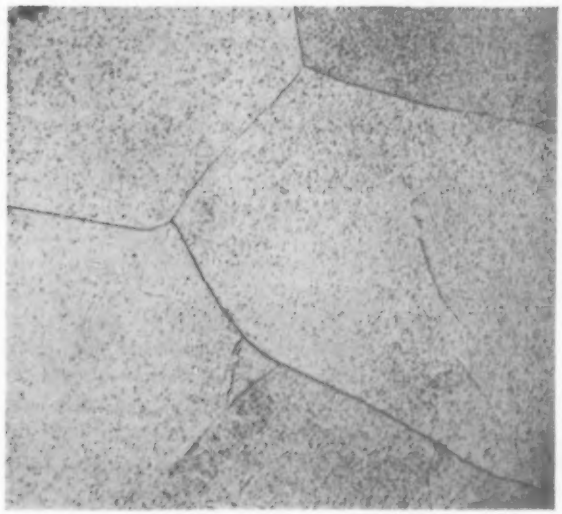

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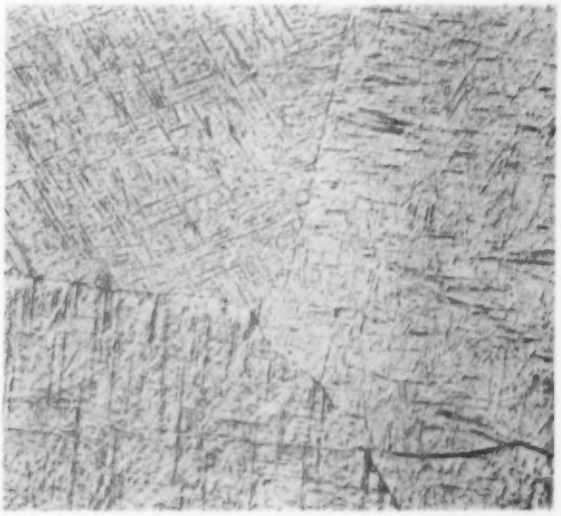
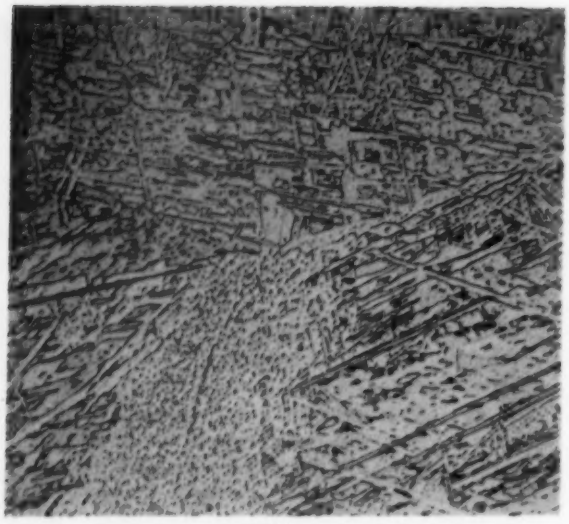


(b)

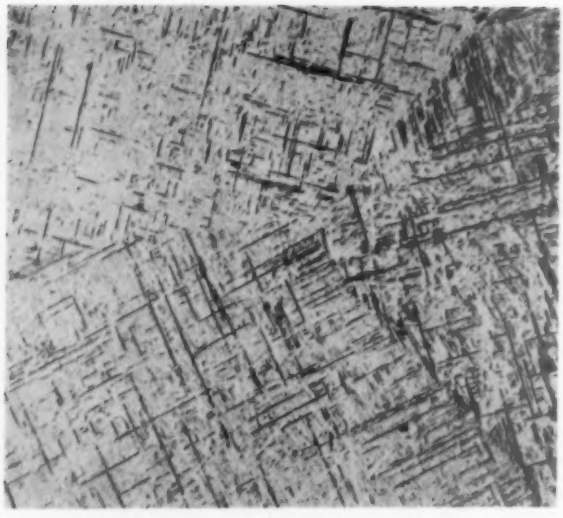
Fig. 23 Typical weld-metal structures in alpha-beta titanium alloys in the as-welded condition

(a) 1–2% beta-stabilizing additions. (b) 3–4% beta-stabilizing additions. (c) 5–6% beta-stabilizing additions. × 250.

Two-point-loaded bend tests were made on most of the alpha-beta alloys which contained combinations of beta-stabilizing elements. These tests showed the same trends as the two-point-loaded bend tests on the titanium-vanadium alloys. Some of the specimens that had brittle weld metals failed in the test. In the others, the deposited weld metals had greater resistance to plastic flow than the base metals or heat-affected zones, and most of the deformation occurred outside of the weld-metal zone. Also, the resistance of the weld metals and heat-affected zones to plastic flow was lowered by heat treatment.



(a)



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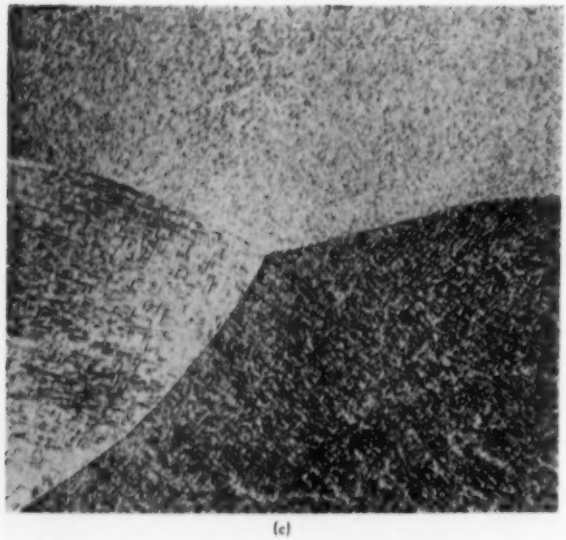

(c)

Fig. 24 Typical weld-metal structures in alpha-beta titanium alloys in the heat-treated condition

(a) 1–2% beta-stabilizing additions. (b) 3–4% beta-stabilizing additions. (c) 5–6% beta-stabilizing additions. × 250.

Hardness and Metallographic Examinations: Hardness tests were made on most of the welded joints in these alloys. They showed the same hardness trends as were observed in the alpha-beta alloys containing only one alloying element. These trends were:

1. The hardness of the weld metals and heat-affected zones in the as-welded joints was higher than that of the base metal.

2. The increased hardness of the weld metals and heat-affected zones became more pronounced as the alloy content increased.

3. Heat treatment lowered the hardness of the weld metals and heat-affected zones until it approached that of the base metal.

The microstructures of the weld metals in these alloys also were similar to those in the binary alpha-beta alloys. Figures 23 and 24 show photomicrographs of the types of structures found in the weld metals of these alloys and how they vary with increasing beta-stabilizing contents.

Weld metals in the alloys which contained about 1 or 2% beta-stabilizing elements generally had coarse acicular transformed-beta structures. These weld metals had good bend ductility and notch toughness in the as-welded and heat-treated conditions. Weld metals in alloys with about 3 to 4% beta-stabilizing elements had finer acicular transformed-beta structures than the alloys with lower beta-stabilizing contents. These weld metals sometimes had good bend ductility and notch toughness in the as-welded condition. However, in some cases, they had low bend ductility as welded. They all had fairly good bend ductility and notch toughness after heat treatment. Weld metals in alloys which contained from 5 to 6% beta-stabilizing elements generally had retained-beta structures with a fine precipitate of alpha. Weld metals with this type of structure practically always had very low ductility as welded. In most cases, they had improved ductility after heat treatment.

Metastable-Beta Alloys. Several metastable-beta alloys containing relatively large amounts of beta-stabilizing elements were prepared for welding tests. They were included in the study because beta alloys have promise for producing weldments with good bend ductility in the as-welded condition and have relatively high tensile strengths.

The metastable-beta alloys are subject to freezing segregation because of high alloy content. Evidence of freezing segregation was found in the metastable-beta alloys studied last year. The metastable-beta alloys studied this year were prepared with combinations of alloying elements intended to minimize freezing segregation.

The compositions and base-metal and weld-joint

Table 6—Base-Metal and Weld-Joint Properties of the Experimental Metastable-Beta Alloys

Alloy Composition, per cent (Balance Titanium)[3]	Unwelded Plates[1]					Welded Plates[2]	
	Ultimate Tensile Strength, psi	0.2% Offset Yield Strength, psi	0.02% Offset Proof Stress, psi	Elongation in 1 Inch, per cent	Bend Ductility, per cent	Longitudinal Bend Ductility, per cent	
						1/8-Inch Sheet	1/4-Inch Plate
9.2 Cr - 8.5 V	129,400[4]	121,900[4]	105,300[4]	12[4]	14[4]	14	14
5.7 Cr - 4.9 Mo	151,800[5]	148,000[5]	131,000[5]	10[5]	11[5]	Brittle	--
4.9 Fe - 7.8 V	144,800[4]	139,000[4]	125,800[4]	15[4]	11[4]	11	Brittle
5.7 Fe - 10.2 V	144,100[5]	139,100[5]	121,000[5]	12[5]	33[5]	4	Brittle

(1) 1/8-inch sheets.
(2) As welded.
(3) Contain residual amounts of other elements.
(4) Heated to 1350 F, 1 hour, furnace cooled to 1150 F, and water quenched.
(5) Beta-quenched heat treated.

properties of the metastable-beta alloys studied during this year's work are listed in Table 6. Either the alpha-beta or beta-quench heat treatment were used on these alloys. The heat treatment which gave the highest base-metal bend ductility was used for the tests. These alloys were prepared as 2- and 3-lb ingots, and consequently the number of tests made on them were limited. A 10-lb ingot of the 9.2% chromium, 8.5% vanadium alloy also was prepared, but it cracked during forging and could not be rolled to plate for welding tests.

The only metastable-beta alloy to produce ductile single- and two-layer weldments in the as-welded condition was the chromium-vanadium alloy. Both of the iron-vanadium alloys produced ductile single-layer welds, but the two-layer welds were brittle. No reason for the difference in ductility in the two welds was found in hardness studies or microstructural examinations.

Microstructural examinations of the weld metals in these alloys showed practically all beta structures with some alpha precipitate. These structures were similar in some respects to the structure of the weld metals in the 20% vanadium alloy shown in Fig. 19.

Aging Tests

A study was conducted to determine the effect of aging on the bend ductility of welds in the alloys. This test was not made in last year's work. In this phase of work, aging tests were made on welded joints in all of the alloys, except the 6.4% aluminum alloy, 20% vanadium alloy and the metastable-beta alloys. There generally was not sufficient plate material of the latter alloys to make these tests.

The aging treatment consisted of storage of the specimens at room temperature and 250° F for 30 days. Aging was evaluated on all welded joints on the basis of longitudinal-bend ductility.

The longitudinal-bend ductility of the welded joints in most of the alloys did not appear to be affected by the aging treatments. In some cases, the ductility was higher after aging than before aging. This was especially true for welded joints in the 1 and 3% aluminum alloys.

The alloys in which aging appeared to lower the longitudinal-bend ductility of welded joints are listed in Table 7. These alloys contained vanadium.

Table 7—Longitudinal-Bend Ductility of Welded Joints Affected by Aging Tests

Alloy	*—Weld-joint longitudinal-bend ductility, %—*		
	Before aging	*After aging*	
		Room temperature	*250° F*
Ti – 5.2% V*	12	6	9
Ti – 9.8% V*	10	6	10
Ti – 11.7% V†	22	16	Brittle
Ti – 3.9% V–4% Al‡	14	10	8

* Heated at 1350° F, 1 hr, slow cooled to 1150° F, and water quenched.
† Water quenched from the beta field.
‡ Tested in as-welded condition.

The results on welds in the 5.2 and 9.8% vanadium alloys indicated that a slight amount of aging might be occurring in the specimens aged at room temperature. However, if age embrittlement were occurring in these joints, it would be expected that more would have occurred in the specimens aged at 250° F than the specimens aged at room temperature.

Welded joints in the 11.7% vanadium alloy were embrittled by aging at 250° F, and the bend ductility was lowered slightly by aging at room temperature. These weldments were water quenched from the beta field prior to aging.

The notch toughness of the weldments affected by aging tests would be expected to be lower after aging than before aging. Notch-toughness tests were not made on specimens after aging at 250° F. However, all of the notch-toughness tests made during this investigation were made at least 30 days after the specimens were welded. For this reason, the notch-toughness data reported earlier would correspond to welded specimens stored for 30 days at room temperature.

Summary

During the course of this investigation, most of the

Table A-1—Heat Treatments Selected for Experimental Alloys

Heat Treatments	Designation	Alloys
Heat to 1600 F, 1 hr, furnace cool to 1300 F, and water quench	No. 1	Ti - 0.8 Mo - 3.8 Al
Heat to 1650 F, 1 hr, and water quench	No. 2	Ditto
Heat to 1700 F, 1 hr, and water quench	No. 3	"
Heat to 1500 F, 1 hr, furnace cool to 1250 F, and water quench	No. 1	Ti - 4 Mn - 4 Al Ti - 3.5 Cr - 2.3 Al Ti - 1.5 Cr - 2.1 Al Ti - 2.9 Mn - 2.8 Al Ti - 3.9 V - 4 Al Ti - 2.75 V - 3 Al
Heat to 1550 F, 1 hr, and water quench	No. 2	Ditto
Heat to 1600 F, 1 hr, and water quench	No. 3	"
Heat to 1400 F, 1 hr, furnace cool to 1200 F, and water quench	No. 1	Ti - 3.6 Cr - 1.9 Fe Ti - 2.2 Cr - 1.5 Fe Ti - 1.7 Cr - 1.7 Fe - 1.5 Mo
Heat to 1450 F, 1 hr, and water quench	No. 2	Ditto
Heat to 1500 F, 1 hr, and water quench	No. 3	"
Heat to 1300 F, 1 hr, furnace cool to 1100 F, and water quench	No. 1	Ti - 1.9 Fe - 3.9 Al Ti - 1.9 Cr - 0.5 Mo Ti - 0.9 Cr - 0.5 Mo
Heat to 1350 F, 1 hr, and water quench	No. 2	Ditto
Heat to 1400 F, 1 hr, and water quench	No. 3	"
Heat to 1300 F, 1 hr, furnace cool to 1100 F, and water quench	No. 1	Ti - 0.9 Cr - 1.0 Fe Ti - 3.7 Cr - 1.5 Mo Ti - 1.9 Fe - 0.9 V Ti - 1.9 Mn - 1.85 Cr Ti - 1.9 Mn - 0.95 Cr Ti - 2 Mn - 1.95 Fe Ti - 1.9 Mn - 1 Fe Ti - 2.35 Mn - 2.7 V Ti - 1.65 Mn - 2 V Ti - 1.6 Mo - 2 V
Heat to 1350 F, 1 hr, and water quench	No. 2	Ditto
Heat to 1400 F, 1 hr, and water quench	No. 3	"

Table A-2—Effect of Heat Treatment on Base-Metal Bend Ductility

Alloy Composition[1] per cent (Balance Titanium)	Bend Ductility, per cent			
	Heat Treatments[2]			Beta Treated Followed by No. 1 Heat Treatment
	No. 1	No. 2	No. 3	
3.5 Cr - 2.3 Al	20	14	8	14
1.5 Cr - 2.1 Al	25	20	11	20
3.6 Cr - 1.9 Fe	33	14	4	11
2.2 Cr - 1.5 Fe	>33	14	Brittle	14
0.9 Cr - 1.0 Fe	>33	>33	>33	14
3.7 Cr - 1.5 Mo	33	25	11	14
1.8 Cr - 0.5 Mo	25	>33	20	--
0.9 Cr - 0.5 Mo	>33	>33	25	25
1.7 Cr - 1.7 Fe - 1.5 Mo	>33	20	14	14
1.9 Fe - 3.9 Al	26	20	25	14
1.9 Fe - 0.9 V	>33	>33	25	14
4 Mn - 4 Al	20	14	14	11
2.9 Mn - 2.8 Al	14	14	11	8
1.9 Mn - 1.85 Cr	>33	>33	20	20
1.9 Mn - 0.95 Cr	>33	33	33	14
2 Mn - 1.95 Fe	>33	25	25	11
1.9 Mn - 1 Fe	25	>33	33	11
2.35 Mn - 2.7 V	>33	25	25	14
1.65 Mn - 2 V	>33	>33	25	20
0.8 Mo - 3.8 Al	25	14	25	11
1.6 Mo - 2.0 V	>33	33	20	20
3.9 V - 4.0 Al	20	20	20	11
2.75 V - 3.0 Al	25	25	20	11

(1) Contain residual amounts of other elements.
(2) Heat treatments described in Table A-1.

Table A-3—Mechanical Properties of the Experimental Alpha-Beta Alloys in Two Heat-Treated Conditions[1]

Alloy Composition,[2] per cent (Balance Titanium)	Heat Treatment: Number 1				Heat Treatment: Number 2			
	Ultimate Tensile Strength, psi	0.2% Offset Yield Strength, psi	0.01% Offset Proof Stress, psi	Elongation in 1 Inch, per cent	Ultimate Tensile Strength, psi	0.2% Offset Yield Strength, psi	0.01% Offset Proof Stress, psi	Elongation in 1 Inch, per cent
3.5 Cr - 2.3 Al	113,500	98,900	87,100	21	134,300	127,300	105,600	9
1.5 Cr - 2.1 Al	103,500	80,500	71,600	24	112,600	90,300	78,800	22
3.6 Cr - 1.9 Fe	136,200	127,500	109,700	22	149,900	140,500	114,800	12
2.2 Cr - 1.5 Fe	128,500	116,700	101,200	24	139,500	121,800	100,000	8
0.9 Cr - 1.0 Fe	93,900	67,300	50,500	28	99,400	70,200	58,900	26
3.7 Cr - 1.5 Mo	120,900	106,300	88,200	26	168,200	156,500	133,300	2
1.8 Cr - 0.5 Mo	98,000	79,400	65,900	27	122,300	98,200	89,500	20
0.9 Cr - 0.5 Mo	93,200	71,700	55,900	28	109,000	86,300	77,700	23
1.7 Cr - 1.7 Fe - 1.5 Mo	120,300	104,700	84,100	24	117,500	105,900	86,000	21
1.9 Fe - 3.9 Al	(3)	(3)	(3)	(3)	(3)	(3)	(3)	(3)
1.9 Fe - 0.9 V	95,300	68,000	54,400	25	100,000	70,300	54,400	15
4 Mn - 4 Al	132,400	120,000	115,500	15	142,500	130,200	124,500	13
2.9 Mn - 2.8 Al	122,900	102,300	91,300	--	133,000	110,800	103,500	15
1.9 Mn - 1.85 Cr	106,000	83,700	62,100	24	115,300	89,400	78,400	27
1.9 Mn - 0.95 Cr	106,200	83,500	72,800	14	109,300	84,400	72,900	29
2 Mn - 1.95 Fe	115,400	86,300	77,900	26	118,800	89,600	74,800	28
1.9 Mn - 1 Fe	103,500	78,800	65,500	24	110,100	83,300	65,200	22
2.35 Mn - 2.7 V	105,000	84,200	74,200	28	114,600	90,500	76,200	22
1.65 Mn - 2 V	98,000	73,700	64,200	27	106,400	78,700	68,200	23
0.8 Mo - 3.8 Al	118,600	97,800	81,800	25	121,000	96,200	85,200	23
1.6 Mo - 2.0 V	96,600	75,800	63,200	29	97,300	76,900	56,500	28
3.9 V - 4.0 Al	107,900	93,200	83,300	18	113,100	87,200	76,500	16
2.75 V - 3.0 Al	108,000	90,700	78,600	21	117,700	88,400	74,600	26

(1) Results from single specimens.
(2) Contain residual amounts of other elements.
(3) Four specimens tested, results varied from 140,000 psi to 105,000 psi.

Table A-4—Base-Metal and Weld-Joint Properties of the Experimental Alpha-Beta Titanium Alloys

Alloy Composition,(2) per cent (Balance Titanium)	Unwelded Plates(1)							Welded Plates					
						Notch Toughness,(2) inch-pounds		Bend Ductility, per cent				Room-Temperature Notch Toughness,(3) inch-pounds	
								Logitudinal		Transverse			
	Ultimate Tensile Strength, psi	0.2% Offset Yield Strength, psi	0.01% Offset Proof Stress, psi	Elongation in 1 Inch, per cent	Bend Ductility, per cent	Room Temperature	-40 F	As Welded	Heat(1) Treated	As Welded	Heat(1) Treated	As Welded	Heat(1) Treated
3.5 Cr - 2.3 Al	113,500	98,900	87,100	21	50	46.5	34.5	2	10	9	20	13.8	18.5
1.5 Cr - 2.1 Al	103,500	80,500	71,600	24	42	44.5	37	10	12	26	34	32.5	31
3.6 Cr - 1.9 Fe	136,200	127,500	109,700	22	60	35.5	16.8	Brittle	8	Brittle	10	3.5	10.5
2.2 Cr - 1.5 Fe	128,500	116,700	101,200	24	58	38	17.5	Brittle	6	6	20	6.5	21
0.9 Cr - 1 Fe	93,900	67,300	50,500	28	48	33	26.5	18	--	42	--	35	--
3.7 Cr - 1.5 Mo	120,900	106,300	88,200	26	18	40	29	Brittle	6	Brittle	10	6	19.5
1.8 Cr - 0.5 Mo	98,000	79,400	65,900	27	50	45.5	41.5	6	8	10	36	24	30
0.9 Cr - 0.5 Mo	93,200	71,700	55,900	28	50	42.5	42	10	15	18	38	30.5	37
1.7 Cr - 1.7 Fe - 1.5 Mo	120,300	104,700	84,100	24	56	56.5	38	Brittle	8	<1	16	6	16.5
1.9 Fe - 3.9 Al	--	--	--	--	30	20.5	16.5	12	10	20	20	31.5	26
1.9 Fe - 0.9 V	95,300	68,000	54,400	25	48	29.5	19.5	18	--	34	--	30	--
4 Mn - 4 Al	132,400	120,000	115,500	15	20	27	13	Brittle	6	Brittle	6	10.5	14
2.9 Mn - 2.8 Al	122,900	102,300	91,300	--	41	33.5	16.5	10	6	18	14	24.5	27
1.9 Mn - 1.85 Cr	106,000	83,700	62,100	24	47	41	34.5	7	7	2	--	20.5	--
1.9 Mn - 0.95 Cr	106,200	83,500	72,800	14	50	36	29	10	--	20	--	34.5	--
2 Mn - 1.95 Fe	115,400	86,300	77,900	26	38	14	11.5	5	10	Brittle	4	14	18.5
1.9 Mn - 1 Fe	103,500	78,800	65,500	24	32	26.5	18.5	11	--	22	--	23.5	--
2.35 Mn - 2.7 V	105,000	84,200	74,200	28	50	56	40	2	10	6	--	16.5	--
1.65 Mn - 2 V	98,000	73,700	64,200	27	50	40.5	41	13	--	16	--	29.5	--
0.8 Mo - 3.8 Al	118,600	97,800	81,800	25	50	31.5	26	8	10	14	20	29	28
1.6 Mo - 2.0 V	96,620	75,800	63,200	29	22	51	59	7	18	10	--	20	--
3.9 V - 4.0 Al	107,900	93,200	83,300	18	28	37.5	32.5	14	--	22	--	30.5	--
2.75 V - 3.0 Al	108,000	90,700	78,600	21	44	35.5	36.5	16	--	24	--	33	--

(1) No. 1 heat treatment.
(2) Contain residual amounts of other elements.
(3) Micro impact specimen.

commercially important titanium alloying elements were studied to determine how they affected the properties of welds in titanium. The investigation has provided a great deal of information which can serve as a foundation for future development work on titanium alloys.

The investigation was primarily concerned with the alloying elements and types of alloys being used commercially. The important findings from the portion of the investigation described in this paper are listed below.

Titanium-Aluminum Alloys

The titanium-aluminum alloys had good weld-joint properties. Welded joints in all of these alloys were ductile and had relatively good notch toughness in the as-welded condition. The notch toughness of the weld metals was higher than the notch toughness of the base metals and exceeded 25 in.-lb over the entire alloy range studied.

The tensile strength of the titanium-aluminum alloys increased from 70,000 psi for the 1.5% alloy to 117,000 psi for the 6.4% alloy, and elongation decreased from 29 to 20%. These alloys had a relatively wide range in tensile strengths and had usable ductility over the entire alloy range studied. However, the 6.4% alloy was difficult to fabricate to plate.

Titanium-Vanadium Alloys

The titanium-vanadium alloys had trends in weld-joint bend ductility and notch toughness similar to those of alloys containing chromium, iron, manganese and molybdenum. For given alloy contents, welded joints in the titanium-vanadium alloys were more ductile and had higher notch toughness than welded joints in the other alloys. However, the vanadium alloys also had lower tensile strengths than alloys containing equivalent amounts of the other beta-stabilizing elements.

Welded joints in the 1 and 2.9% vanadium alloys had good bend ductility and very good notch toughness in the as-welded condition (35 in.-lb). Also, welded joints in the 5% vanadium alloy had almost as good bend ductility and notch toughness as welded joints in alloys containing only 3% of the other beta-stabilizing elements.

Welded joints in the 9.8 and 11.7% vanadium alloys had low ductility as welded, but had good ductility after heat treatment. Welds in alloys containing equivalent amounts of the other beta-stabilizing elements generally had low ductility both as welded and heat treated.

Single-layer weldments in the 20% vanadium alloy were ductile as welded and heat treated. Two-layer welded joints in this alloy were brittle as welded but had good ductility after heat treatment.

The titanium-vanadium alloys generally had lower tensile strength than alloys containing chromium, iron, manganese or molybdenum. The tensile strength of the 5% vanadium alloy (100,000 psi) was about the same as the tensile strength of alloys containing only 3% of the other elements.

Alpha-Beta Alloys Containing Combinations of Alloying Elements

Alpha-beta alloys which contained aluminum appeared promising for welding applications. Aluminum additions increased the strength of the alloys without lowering their weld-joint properties appreciably. With aluminum additions, alpha-beta alloys developed tensile strengths exceeding 120,000 psi attended by good ductility and notch toughness in the as-welded condition.

Welded joints in alloys containing combinations of two or more beta-stabilizing elements had bend ductility and notch-toughness properties similar to those of alloys containing only one element. No combinations of beta-stabilizing elements were found to produce alloys with base-metal and weld-joint properties superior to those of alloys containing other beta-stabilizing elements.

The bend ductility and notch toughness of the welded joints in the alpha-beta alloys appeared to depend more on the amounts of beta-stabilizing elements present in the alloys than on the choice of beta-stabilizing elements used in alloying.

Metastable-Beta Alloys Containing Combinations of Alloying Elements

A metastable-beta titanium alloy containing 9.2% chromium and 8.5% vanadium produced ductile single- and two-layer welded joints. However, the tensile strength (129,000 psi) of this alloy was too low to warrant use of so much alloying element. Two other alloys containing iron and vanadium produced ductile single-layer joints. However, two-layer joints in these alloys were brittle.

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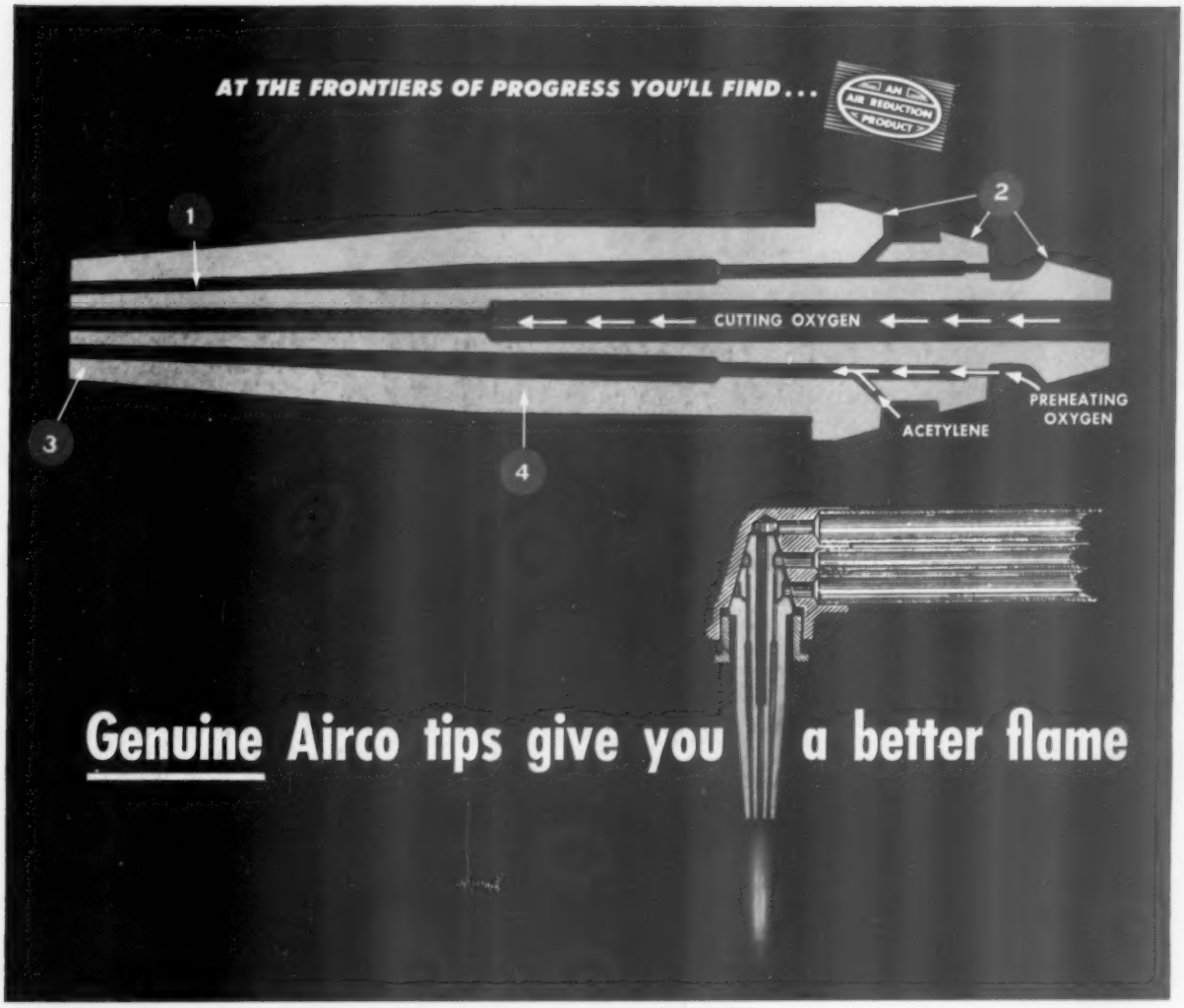

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